HIGH SPEED DYNAMO ELECTRIC MACHINERY

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PREFACE.

WITH the recent extensive introduction of high speeds with steam turbines and hydraulic turbines, the importance of thoroughly investigating the influence of such speeds on the design of dynamo electric machinery is quite obvious, and has been the primary motive in writing the present treatise. The influence of the rated speed is, however, so closely associated with that exerted on the design by the rated output and pressure, that we have found it necessary to carefully consider all three factors.

It would be very difficult by customary methods, to disentangle the consequences of these various influences, but by the novel methods to which we have resorted, a clear comprehension of the subject, and the deduction of perfectly definite conclusions have been made practicable. Incidently we should like to call attention to these designing methods as affording fairly certain means of arriving at a more satisfactory design than is likely to be obtained by the use of less practical and less logical methods.

Developments in the design and construction of this class of machinery have been very rapid, and considerable progress has been made even during the last few months. The designs and constructions described in this treatise comprise, not only the standard methods by which the especial conditions imposed by high speeds are being met, but also various recent propositions which have not yet withstood the test of time.

The detailed studies of the influences of the various elements in the design, especially of the rated speed, indicate the lines which must be followed, not only in the electromagnetic design, but also in dealing with the constructional problems.

In the preparation of this treatise courtesies have been extended to us by many manufacturing firms, both in their corporate capacity, and through their officials or designers, and we wish to cordially

express our appreciation of these courtesies. We believe the following to be a complete list:

Messrs. Allgemeine Elektricitäts Gesellschaft.
British Thomson Houston Company.
British Westinghouse Company.
Brown Boveri & Co.
Bruce Peebles & Co.
Brush Electrical Engineering Company.
Bullock Electrical Manufacturing Company.
Dick, Kerr & Co.
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Kolben & Co.
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Le Carbone Company.
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Siemens Schuckert Works.

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The Institution of Electrical Engineers.
The American Institute of Electrical Engineers.
Electrical Review (England).
Electrical Review (New York).
Electrical Engineering.
Electrical Times.
Electrician.
Electrical World.
Elektrotechnische Zeitschrift.
Elektrotechnik und Maschinenbau.
Street Railway Journal.

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"HIGH SPEED DYNAMO ELECTRIC MACHINERY."

LIST OF SPECIFICATIONS OF DESIGNS GIVEN IN THIS TREATISE.

Part II. Alternating Current Generators.

Kva.	Phase.	Poles.	R.P.M.	Cycles.	Volts.	Chapter.	Page.
1375	3	64	94	50	5500	VI	95
1500	3	6	1000	50	11000	VI	95
650	3	4	1500	50	500	VII	127
400	3	64	94	50	3000	VIII	133
400	3	4	1500	50	3000	VIII	133
500	3	2	3000	50	550	VIII	133
500	3	2	3000	50	550	VIII	133
3000	3	4	750	25	11000	IX	168
3000	3	6	500	25	11000	IX	168
3000	3	8	375	25	11000	IX	168
3000	3	12	250	25	11000	IX	168
3000	3	24	125	25	11000	IX	168
3000	3	36	83	25	11000	IX	168
3000	3	4	1500	50	11000	IX	184
3000	3	6	1000	50	11000	IX	184
3000	3	8	750	50	11000	IX	184
6000	3	4	.750	25	11000	X	201
6000	3	6	750	37.5	11000	X	201
6000	3	8	750	50	11000	X	201
1500	3	8	750	50	11000	X	211
1000	3	4	1500	50	2000	XI	276
250	3	2	3000	50		XI	277
1000	1	4	1500	50	5200	XI	278
1500	3	6	1000	50	11000	XI	284
500	3	4	1500	50	550	XI	298

Part III. Continuous Current Generators.

Kw.	Poles.	R.P.M.	Volts.	Chapter.	Page.
250	10	125	250	XV	368
	8	250			
	6	500			
	6	1000			
	4	2000			
	2	3000			
500	14	125	500	XV	368
	10	250			
	8	500			
	6	1000			
	4	2000			
	4	2500			
1000	16	125	1000	XV	368
	12	250			
	8	500			
	6	1000			
	4	2000			
1000	6	1000	1000	XVII	398
1000		1500	550	XVII	405
750	4	1600	500	XVIII	441
750	6	1500	250	XVIII	445
375	4	2500	240	XVIII	448

HIGH SPEED DYNAMO ELECTRIC MACHINERY.

PART I — GENERAL CONSIDERATIONS.

CHAPTER I.

INTRODUCTORY.

In the early days of the development of dynamo electric machinery, when designs of only very small output came into consideration, it was found that with increase in the rated speed, the "weight efficiency" and the "cost efficiency" of dynamo electric generators continued to increase up to the limit to which it was found practicable to increase the speed with due consideration to the design of the prime mover. In order to obtain these advantages of high speed it was very customary to drive the electric generator by belting or by ropes, at a speed considerably higher than that of the engine from which it was driven. The disadvantages of rope or belt driving led, notably in England, to the development of the so-called "high speed" engine to which the generator was directly coupled. As development proceeded, the most customary rated capacity required for a single generating set, rapidly increased, and while the higher speeds continued to be found advantageous for alternating current generating sets, even when of large rated capacity, it gradually became apparent that for continuous current generating sets of large rated capacity, such disadvantages relating to commutation and ventilation attended the design, that but little if any advantage remained from the use of high speeds. It has finally come to be realized by those best informed, that in the case of continuous current generators for the customary voltages of from 500 to 650 volts, there is, for each rated

capacity, a limiting speed beyond which the design becomes not only less satisfactory, but also more expensive.

The impression which nevertheless still prevails in less well informed circles, that continuous current generators are especially adapted to high speed work, and that the development of low speed engines is not in the interests of the best dynamo design, but is a concession to the advantages of low speed for engines, is far from correct. Low and high speed steam engines have their respective merits and demerits: the same is true of low and high speed dynamo electric machinery, and it is found necessary to consider each case very carefully. It may be said in general that, up to a certain limit, a machine for a given capacity and voltage, and of a given degree of excellence, will be cheaper the higher the speed; but for speeds above that limit, the machine will be more expensive. Hence it may be said that for each rating there is a particular speed which is the most economical and satisfactory speed. Analogous considerations lead the steam engineer to prefer a particular speed for a steam engine of a given rated output. Just as the experience of each steam engineer with the type of engine with which he has been chiefly engaged leads him to an individual opinion as to the most favourable speed for a steam engine of a given capacity, so one finds considerable difference of opinion among designers of electrical machinery as to the most favourable speeds for continuous current dynamos.

At this point it is desirable to consider the relation of the angular speed to the output, in prime movers of the types which are in general use for electric power generation purposes. The curves in Fig. 1 give a rough idea of the speeds of prime movers of the various types for rated outputs up to 6000 kw. In these rapidly developing lines of work, manufacturing concerns are constantly modifying their plans. Hence while the turbine curves in Fig. 1 reflect the general state of affairs, it would be imprudent to place reliance on the precise values to which the curves are plotted. The point to be observed from these curves is the large range of speeds lying between the speeds of steam turbines and the speeds of piston engines.

Speeds within this range are consequently not available for direct coupled electric generators, except when driven from hydraulic turbines. Such speeds are nevertheless often more suitable and economical for electric generators, as we shall see in the course of this work.

Fig. 2 gives a corresponding curve for the de Laval turbine which has been plotted separately since the output for which this type of turbine is made does not usually exceed 200 kw. This limitation is chiefly due to the reduction gear which is employed to reduce the speed in the ratio of 10:1. The primary turbine spindle runs at speeds of from 10,000 R.P.M. to 30,000 R.P.M; the secondary shaft to which the dynamos are coupled running at speeds ranging from 1000 to 3000 R.P.M. It is with the influence of the rated speed on the design and characteristics of electrical machinery that we are here chiefly concerned.

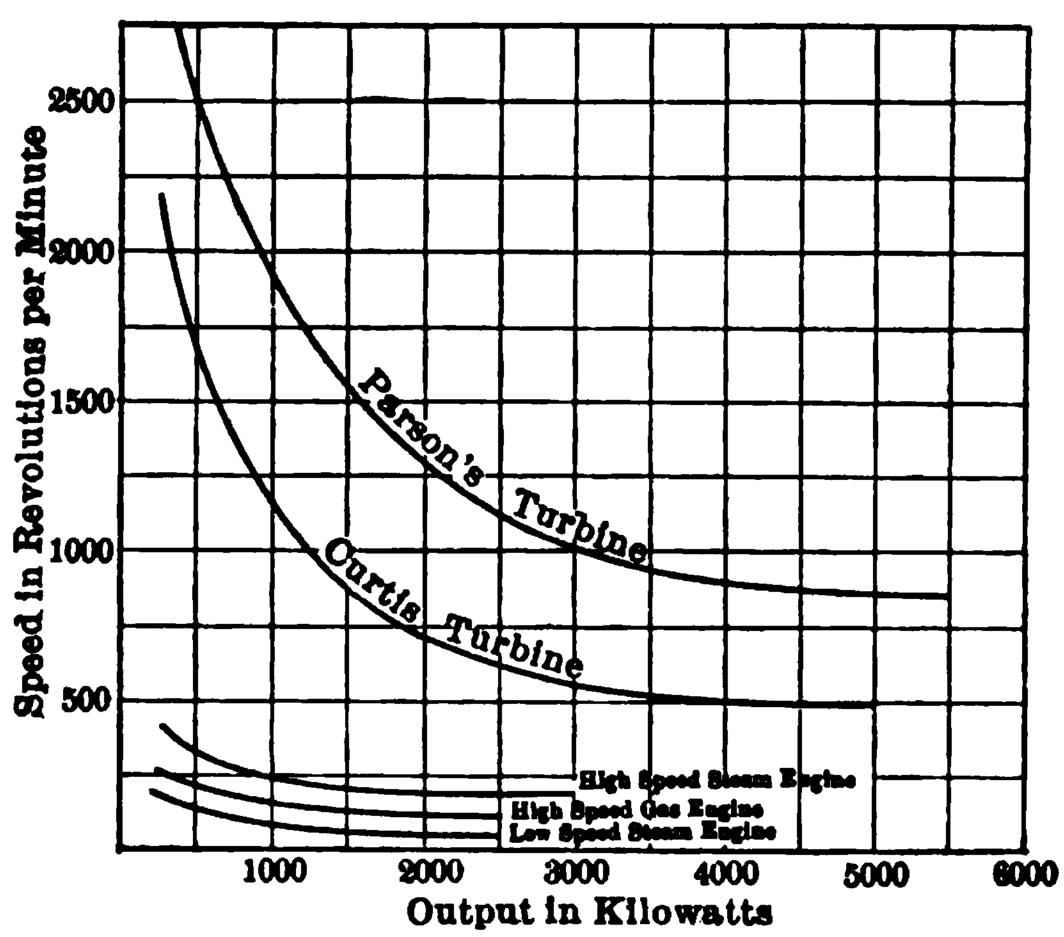


Fig. 1.— Curves showing relation of rated speeds and rated outputs for steam turbines and other prime movers.

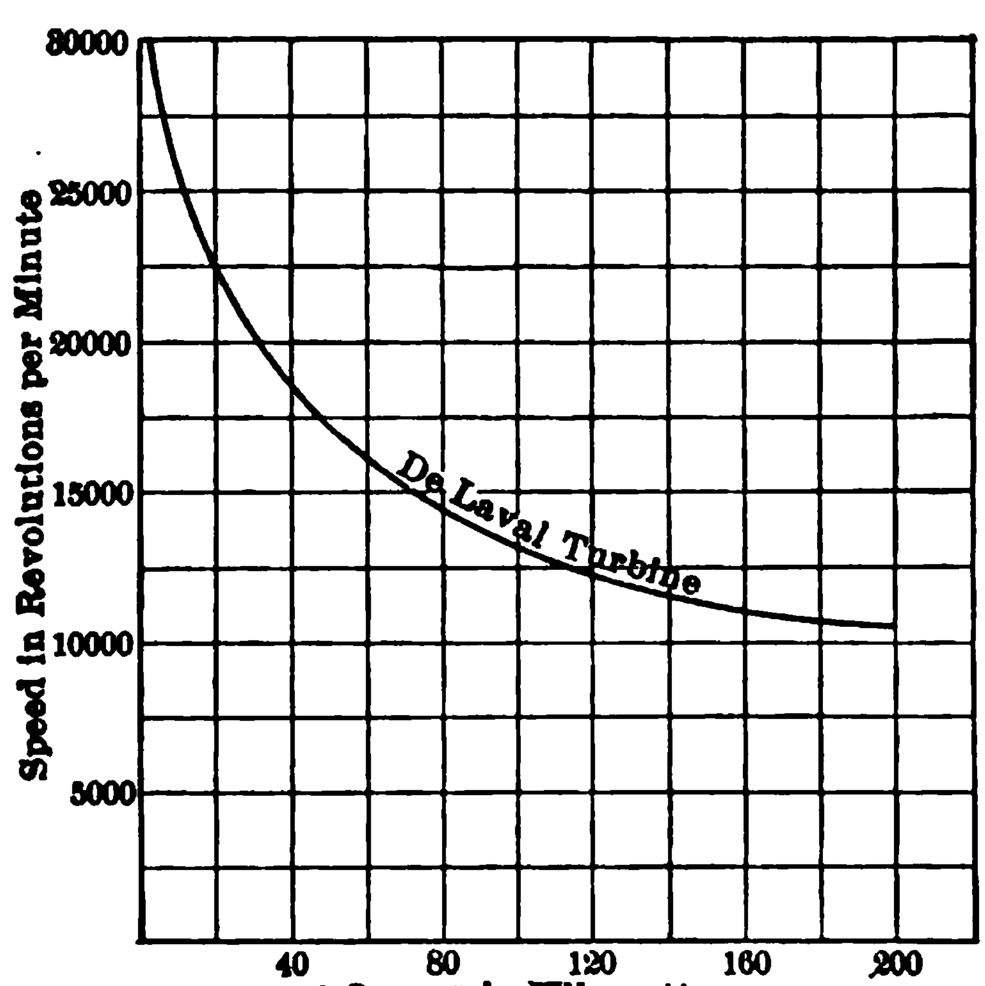


Fig. 2.—Curve showing relation of rated speeds and rated outputs for de Laval turbines.

It would probably have been more correct to entitle this treatise " The influence of the rated speed *and output* on the design of electric machinery," for it is impracticable to advantageously discuss the influence of the speed without also discussing the influence of the rated output.

Two broad dividing lines are at once encountered when approaching the subject, for a comparatively superficial examination reveals the three following facts:

1. When not carried to excess, the lower the speed in revolutions per minute, the more satisfactory will be the results which may be obtained in designing continuous current dynamo electric machinery.

2. When not carried to excess, the higher the speed in revolutions per minute, the more satisfactory will be the results which may be obtained in designing alternating current dynamo electric machinery.

For either one of these two classes of dynamo electric machinery, a machine for some particular rated output and voltage will yield satisfactory results for a minimum Total Works Cost when designed to operate at some particular speed in revolutions per minute. For higher and lower speeds, either the Total Works Cost will be greater for a given quality of performance, or the performance will be inferior for a given Total Works Cost.

3. For a given rated output, this preferable speed will be much lower for a continuous current design than for an alternating current design.

There are exceptions to all rules, nevertheless the above conclusions are of fairly general applicability. The working out of the large number of designs, of which the results are given in this treatise, was undertaken largely with the object of attaining to greater precision in our knowledge of the influence exerted on the design by these two factors of speed and output.

While a considerable knowledge on the part of the reader as regards the detailed steps employed in the design of electric machinery has often been assumed, the preparation of this treatise has afforded a favourable opportunity of setting forth a number of modified processes which we have found it useful to employ at various stages of the design. These processes are generally of an empirical nature, but readers conversant with the principles of the design of dynamo electric machinery may be glad to substitute them for the more elementary, although in some cases more complex, processes set forth in most text-books dealing with the subject.

In view of the broad demarcation to which we have alluded, we have devoted separate sections to the treatment of alternating current and continuous current machinery; but we have, for the sake of variety and also of departing from a meaningless tradition, given first place to the treatment of alternating current machinery.

Part I, entitled "General Considerations," contains Chapters I to

IV which deal with matters common to both classes of machinery. Chapter IV on "Materials" might profitably be again read after a perusal of the whole work, and especially of Chapters XI, XII, and XVIII which deal with the constructional problems. The question of materials is closely allied to the special types of construction employed and it was difficult to decide whether to place the chapter near the beginning or at the end of the book.

Part II, entitled "Alternating Current Generators," comprises Chapters V to XII which deal with alternating current machinery, and Part III, entitled "Continuous Current Generators," comprises Chapters XIII to XIX dealing with continuous current machinery.

CHAPTER II.

DESIGN COEFFICIENTS FOR DYNAMO ELECTRIC MACHINERY.

DESIGN Coefficients are of use in connection with the design of dynamo electric machinery as constituting convenient starting points on which to base a new design, or for purposes of comparisons between several designs. Such coefficients may embody relations between:

A. Dimensions and output — designated "Output Coefficients."

B. Weight and dimensions or output — "Weight Coefficients."

C. Cost and dimensions or output — "Cost Coefficients."

A.

Output Coefficients.

The following output coefficients have been more or less extensively used:

(1) $\dfrac{W}{D^2\lambda gR}$

(2) $\dfrac{W}{D\lambda g}$

(3) $\dfrac{W}{\text{Volume of active belt}}$

W = rated output in watts.
D = armature diameter at air gap.
λg = armature gross core length.
R = rated speed in R.P.M.

(4) Specific Utilization Coefficients.*

Of the above, (1) is the most convenient and is the most extensively used. We shall designate this coefficient as simply the "Output Coefficient" and shall denote it by the letter ξ.

Hence $$\xi = \frac{W}{D^2\lambda gR}.$$

D and λg are expressed in decimetres, W in watts, and R in revolutions per minute.

* The specific utilization coefficients which are developed in this Chapter were originally proposed by Dr. S. P. Thompson. *See* Seventh Edition (1904) of Dynamo Electric Machinery, (E. & F.N. Spon. London.) Vol. I. p. 575.

In the case of alternating current machines the output in volt amperes should be employed for W instead of the output in watts. This is equal to nVI where n denotes the number of phases, V the voltage per phase, and I the current per phase.

We may obtain an insight into the inter-relation of these coefficients and a comprehension of their significance, by developing certain fundamental relations for dynamo electric machines. We shall at this point carry out such a development for alternating current machines. For continuous current machines the relations are precisely similar.

The primary fundamental equation for alternating current machines is

$$V = \frac{kTMN}{10^8} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where V = the voltage per phase,

k = the "voltage coefficient" the values of which are given in Chapter VII, page 110.

T = the number of turns in series per phase.

N = the frequency in cycles per second.

M = the magnetic flux entering the armature per pole in *c. g. s.* lines.

If we also write

p = the number of poles.

I = the full load current per phase.

R = the rated speed in revolutions per minute;

then multiplying both sides of equation (1) by the current I, we have

$$VI = \frac{kMNIT}{10^8} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (2)$$

We have also the fundamental relation between the speed, frequency, and number of poles,

$$N = \frac{pR}{2 \times 60} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (3)$$

Hence,

$$VI = \frac{kMIT \times pR}{2 \times 60 \times 10^8} = \frac{k \times pM \times IT \times R}{120 \times 10^8} \quad . \quad . \quad . \quad (4)$$

This equation shows that the output per phase VI depends on the three terms pM, IT, and R: pM is the total flux crossing the air gap from all poles from field system to armature, IT is the ampere turns per phase, which is equal to the total ampere turns on the armature for all phases divided by the number of phases, and R is the speed in revolutions per minute.

Thus for a given machine of given size (i.e., dimensions) the output will be practically proportional to the speed. Doubling the speed of a given machine doubles the frequency, which practically doubles the voltage, and, for a given current output, the output in kilo-volt-amperes will be doubled. Practical considerations which are set forth later in his treatise, occasion wide deviations from direct proportionality between speed and output. Conversely a machine for a given rated output will usually be smaller (in dimensions) the higher the speed.

We may further develop equation (4) as follows: Let us designate,

β = the average magnetic flux density in the air gap, or the "specific magnetic loading" of the armature.

α = the ampere conductors per centimetre of armature periphery, or the "specific electric loading" of the armature.

n = the number of phases.

β is equal to the Total Flux divided by the total air gap surface.

$$\therefore \ \beta = \frac{pM}{\pi D \lambda g} = \frac{M}{\tau \lambda g}, \text{ for } \tau, \text{ the polar pitch, is equal to } \frac{\pi D}{p}$$

α is equal to the total armature ampere conductors divided by the armature periphery at the air gap $= \dfrac{nIT \times 2}{\pi D}$.

$$M = \beta \tau \lambda g.$$

Also the armature ampere turns per phase,

$$IT = \frac{\pi D \alpha}{2n}.$$

Substituting these in equation (4) we obtain

$$VI = \frac{k \times p\tau\lambda g\beta \times \pi D\alpha \times R}{120 \times 10^8 \times 2n}.$$

Multiplying by n we obtain the total output of the machine in volt amperes,

$$nVI = \frac{kp\pi D\lambda g\tau\alpha\beta R}{240 \times 10^8} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad . \qquad (5)$$

We also have $\tau = \frac{\pi D}{p}$ and substituting this in equation (5) we obtain

$$nVI = \frac{k\pi^2}{240 \times 10^8} D^2\lambda g\alpha\beta R$$
$$= 0.041 \times 10^{-8} kD^2\lambda g\alpha\beta R \quad . \quad . \quad . \quad . \qquad (6)$$

k varies with the ratio of pole arc to pole pitch, and with the spread of the armature winding (see page 110), but if we take for the moment the constant value, $k = 4.44$, we have

$$nVI = 0.182 \times 10^{-8} D^2\lambda gR\alpha\beta. \quad . \quad . \quad . \quad . \quad . \qquad (7)$$

This is a rational formula connecting the output, speed, and the principal dimensions, or the cubical volume enclosed by the armature surface at the air gap which is proportional to $D^2\lambda g$.

From equation (7), for a given rating and speed, it is seen that the dimensions ($D^2\lambda g$) will be dependent on the two factors α and β — which are respectively the "density" of ampere conductors on the periphery of the armature, and the density of the flux in the air gap, — and clearly the higher the value of either or both of these factors, the smaller will be the machine. In practice the values for each of these are somewhat of the same order for all normally designed machines, their precise values depending on the conditions to be fulfilled by the particular case under consideration.

The values are limited by consideration of the permissible heating (the dimensions and surface of the machines must be sufficiently large to radiate the heat due to losses, without undue temperature rise), and by pressure regulation in the case of alternators and by commutation in the case of continuous current machinery.

If we transpose equation (7) we may obtain

$$\frac{nVI}{D^2\lambda gR} = 0.182 \times \alpha\beta \times 10^{-8}.$$

The expression on the left hand side is the output coefficient ξ as defined at the beginning of this chapter on pages 6 and 7.

Hence the value of ξ is

$$\xi = 0.182 \times \alpha\beta \times 10^{-8}.$$

In commencing the carrying out of a design, it is convenient to assign an appropriate value to ξ and to derive from this the value of $D^2 \lambda g$ and thence the chief dimensions.

We shall in later chapters give considerable attention to the coefficients α and β in connection with the various designs studied. For the present purpose we shall analyse the value of the output

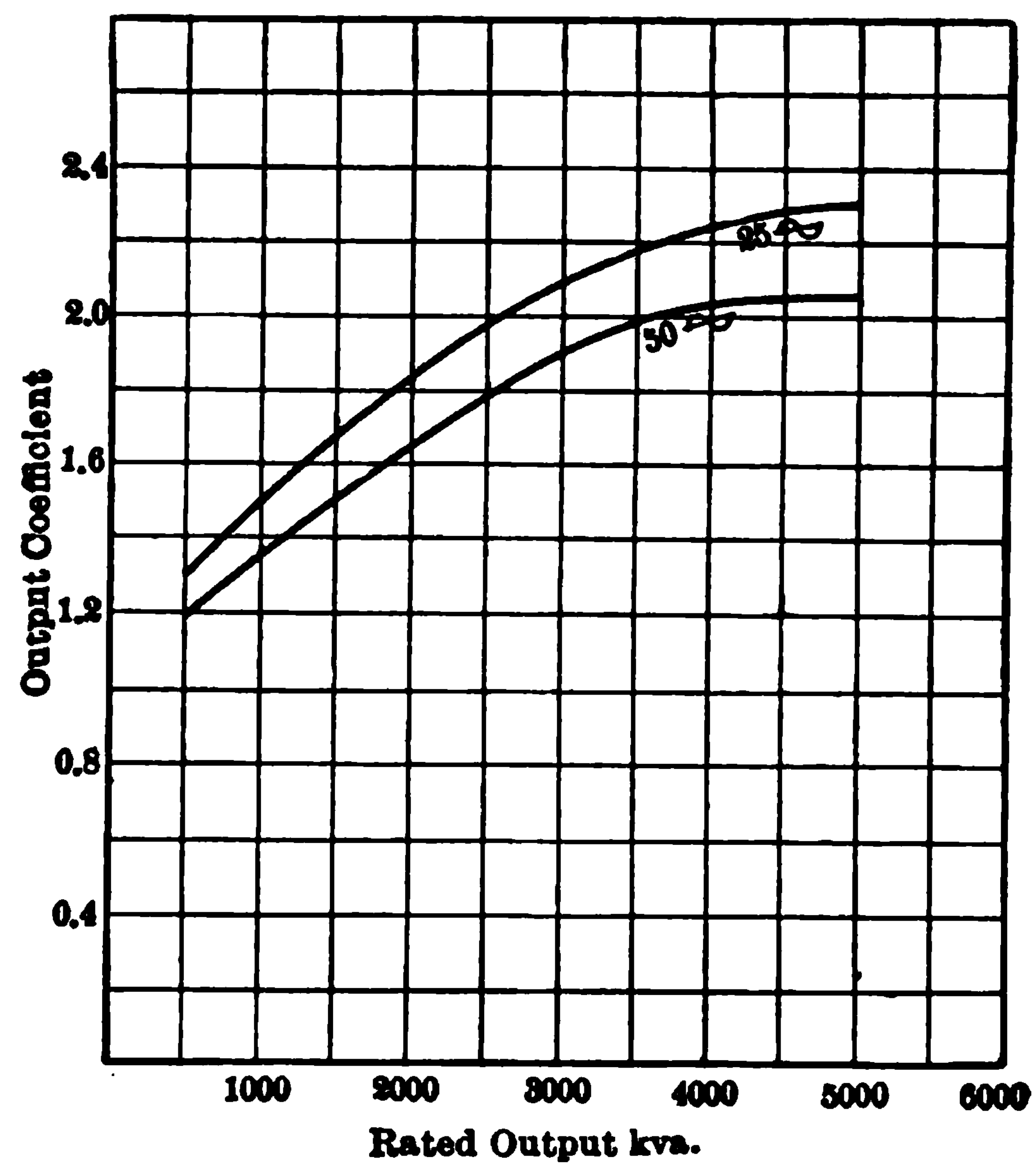


FIG. 3.— Output coefficients of low and moderately low speed alternators.

coefficient ξ, and the general influence on this value, exerted by the various factors in the design such as speed and frequency.

The curves which follow may be taken as giving rough but fairly representative average values for ξ in normal designs, and also as indicating in what cases, and to what extent, higher values may be obtained.

OUTPUT COEFFICIENT OF ALTERNATORS.

The curves in Fig. 3 show the relation between the average output coefficient and the rated output of polyphase alternators. The data from which these curves are drawn is chiefly for low speed machines and the curves should be taken as roughly representative

for low and moderately low speed alternators. The question of the influence of high speeds is considered farther on.

Fig. 4 shows the relation between the average output coefficient and the air gap diameter for low speed and moderately low speed alternators. It is not difficult to obtain good designs with output coefficients higher than the values indicated by the curves in Figs. 3 and 4, but as these curves represent the bulk of the designs from which the analysis has been made, they will be considered as representative values, and the further studies in this chapter will have reference to them.

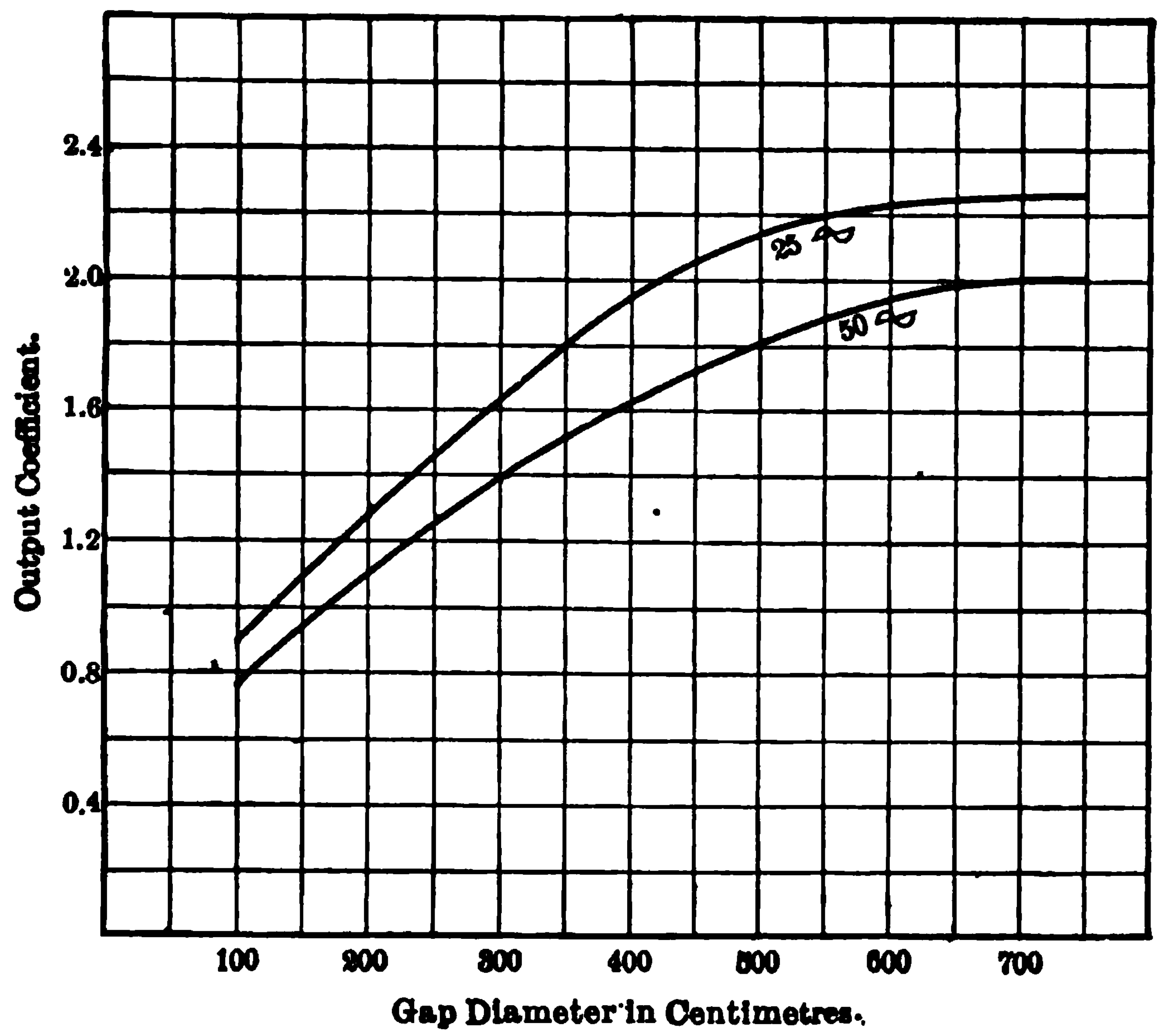


FIG. 4.— Output coefficients of alternators.

It does not invariably follow that a higher output coefficient corresponds to a lower Total Works Cost, although generally speaking the higher the value of ξ the lighter and cheaper the machine.

The curves of Figs. 3 and 4 should be taken as mean curves for machines within the average range of frequency, and as roughly indicating the influence of the latter on the value of the output coefficient. Fig. 3 gives two curves for 25 and 50 cycle alternators respectively, showing the output coefficient plotted against the rated output. It will be noted that somewhat higher output coefficients are obtain-

able with lower frequencies. Fig. 4 shows corresponding curves in which ξ is plotted against the air gap diameter for 25 and 50 cycle machines.

Regarding the influence of the speed, although the weight of the machine is considerably reduced by the employment of high speeds (the weight being an inverse function of the speed for a given rated output), it is nevertheless not the case that high speeds permit of higher output coefficients. On the contrary, the output coefficients obtained with steam turbine alternators are considerably lower than for slow speed machines. This statement is borne out by inspection of any

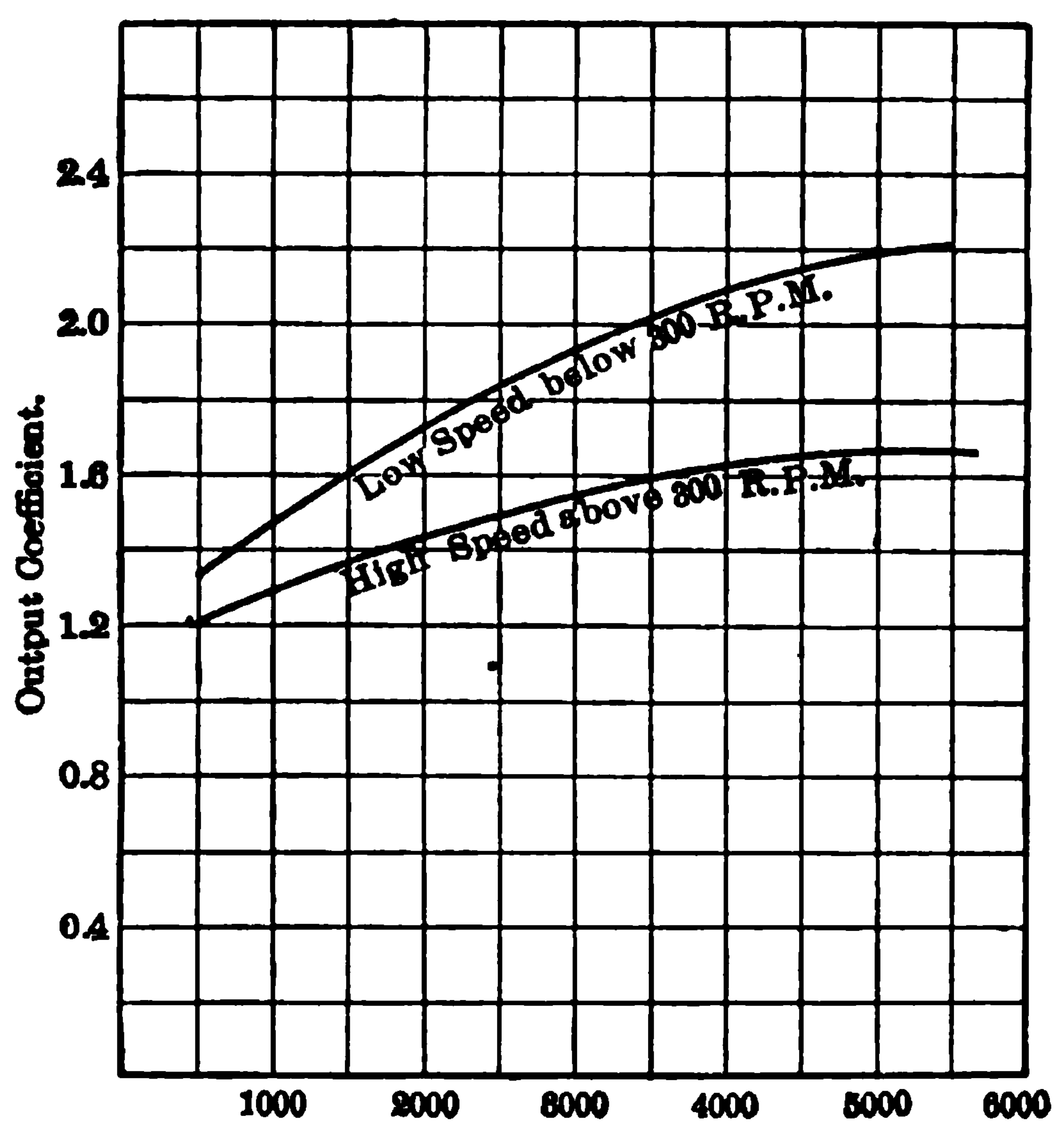


FIG. 5.— Output coefficients of alternators.

turbo-alternator designs. It is due to the fact that the design of alternators for high speeds is a difficult problem, and larger dimensions than would otherwise be required are called for, chiefly on account of the question of heating. We have in Chapters VI, VIII, IX, and X studied more specifically the reasons for these results.

In Fig. 5 the upper curve applies to low speed machines, say below 300 R.P.M. The lower curve gives a rough idea of the average output coefficients for high speed machines above 300 R.P.M., and up to a maximum of 1500 R.P.M.

The low output coefficients for the high speed machines are apparent from these curves. In smaller ratings, the curves more nearly approach

one another, as the high speeds are more favourable to small machines than to machines of large rating.

In Fig. 6 the output coefficient is shown as a function of the air gap diameter for high and low speeds. The high speed curve, it will be seen, is above the low speed and it may be thought at first sight that this is contradictory to the curves in Fig. 5. This is not the case; the fact is that, for a given *diameter*, the high speed machine has a higher output coefficient than the low speed. This is because for any given diameter a high speed machine would have a rated out-

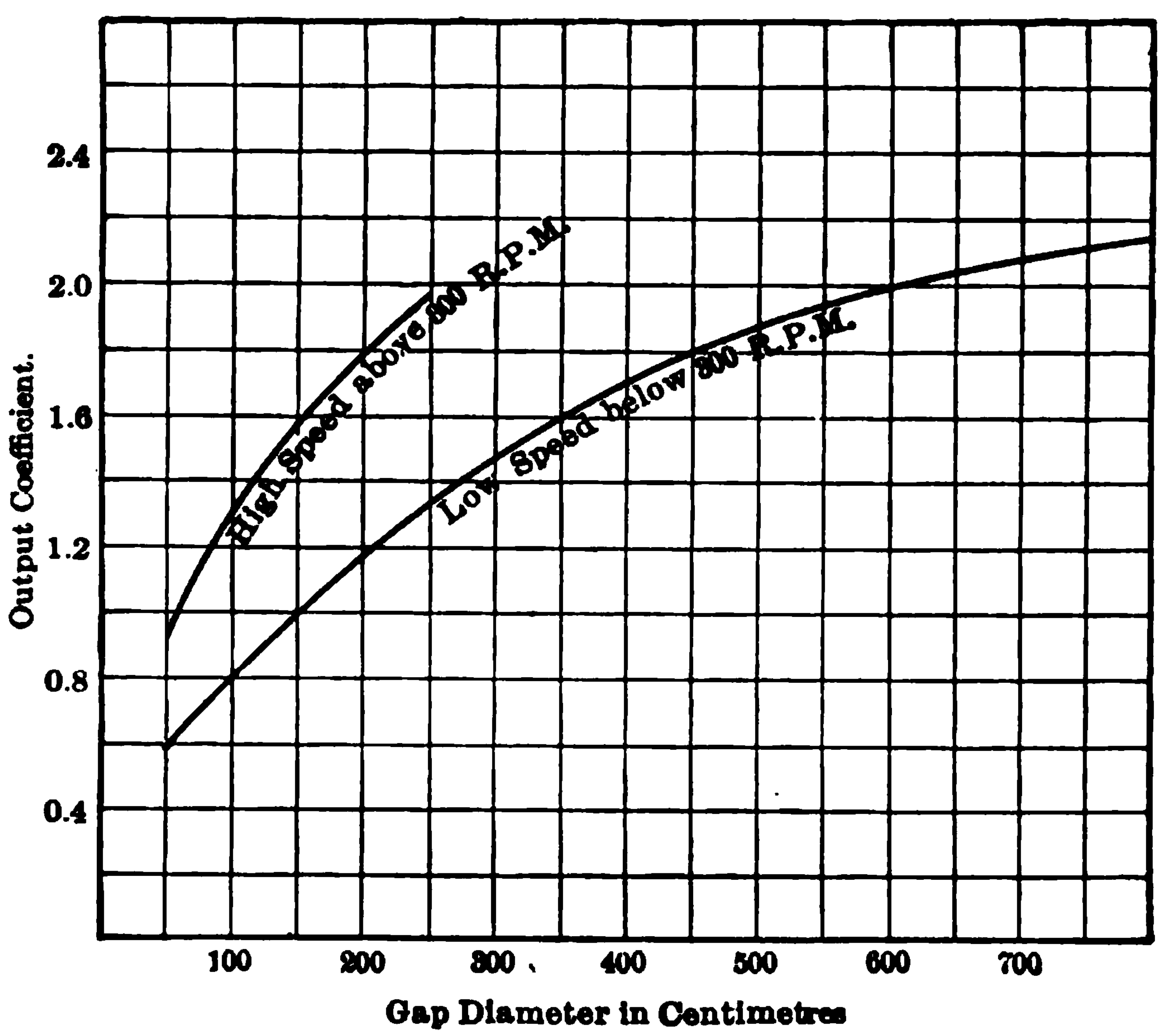


FIG. 6.— Output coefficients of alternators.

put much greater than that of the corresponding low speed machine. Thus, for example, let us consider a 3000 kva. machine designed first for a speed of 100 R.P.M. and secondly for 1000 R.P.M. At 100 R.P.M. the output coefficient according to the upper curve of Fig. 5 would be about 1.95; and at 1000 R.P.M., from the lower curve of Fig. 5 the output coefficient would be 1.55.

In such a pair of machines the air gap diameter would be about

600 centimetres for the 100 R.P.M., and
150 centimetres for the 1000 R.P.M.

(see curves in Fig. 70, Chapter VII).

Now from the curves of Fig. 6 the output coefficients corresponding to these diameters are respectively

2.0 for the low speed machine of 600 centimetres diameter and
1.6 for the high speed machine of 150 centimetres diameter.

These figures are in good agreement with those obtained from the high and low speed curves of Fig. 5.

OUTPUT COEFFICIENT OF CONTINUOUS CURRENT MACHINES.

If, for continuous current machines, we carry out a similar investigation, we arrive at the following expression for the output coefficient:

$$\xi = 0.160 \times 10^{-8}\alpha\beta.$$

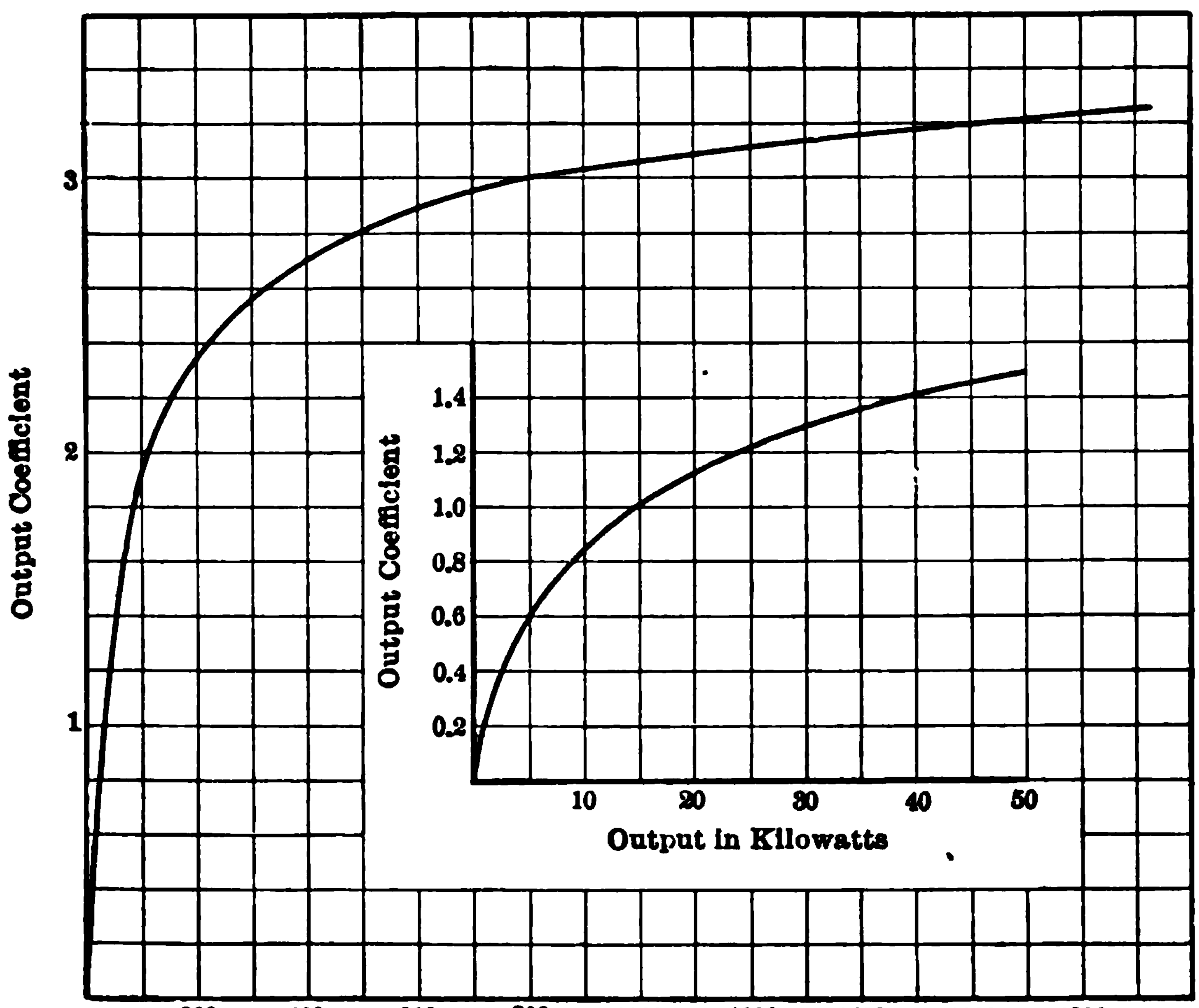


FIG. 7.— Output coefficients of continuous current machines.

The numeral is in this case 0.160 against 0.182 for alternators. This is owing to the fact that the voltage coefficient k is 4.00 for continuous current machines while it was taken as 4.44 for alternators. It must not be taken from this that the values of ξ obtainable with contin-

uous current machines are lower than those for alternating current machines. On the contrary, the very reverse is generally the case as will be seen from the curves which will be given.

That higher output coefficients may be obtained with continuous current machines, signifies that the values of either α or β, or both, are higher than in alternators. The difference is accounted for by the stringent restrictions, chiefly as regards pressure regulation, en-

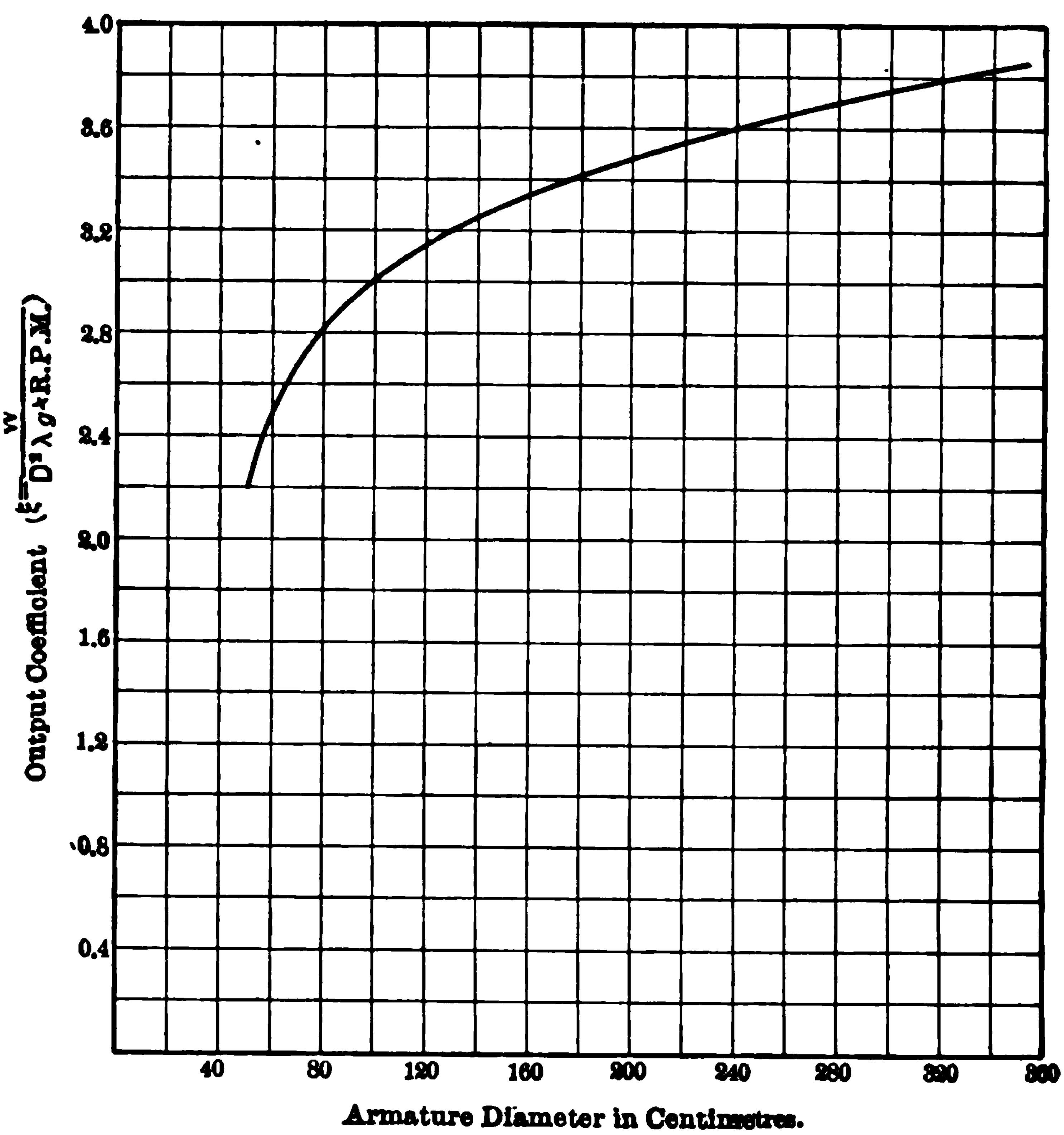


FIG. 8.— Output coefficients of continuous current machines.

countered in the case of alternators. Figs. 7 and 8 give curves for the average values of the output coefficient for continuous current machines plotted as a function of the rated output and diameter respectively.

In continuous current machines, it again appears that designs for higher rated speeds do not permit of such high values for ξ as

lower speed designs. This is established by the studies in Chapters XV and XVI, and the quantitative relations obtained from these results are shown in the curves in Fig. 9. These curves show ξ as a function of the rated output for rated speeds ranging from 125 to 2000 R.P.M.

The values indicated by these curves correspond to normal designs. Here again it does not follow that in all cases a high output coefficient corresponds to the most suitable or to the cheapest designs.

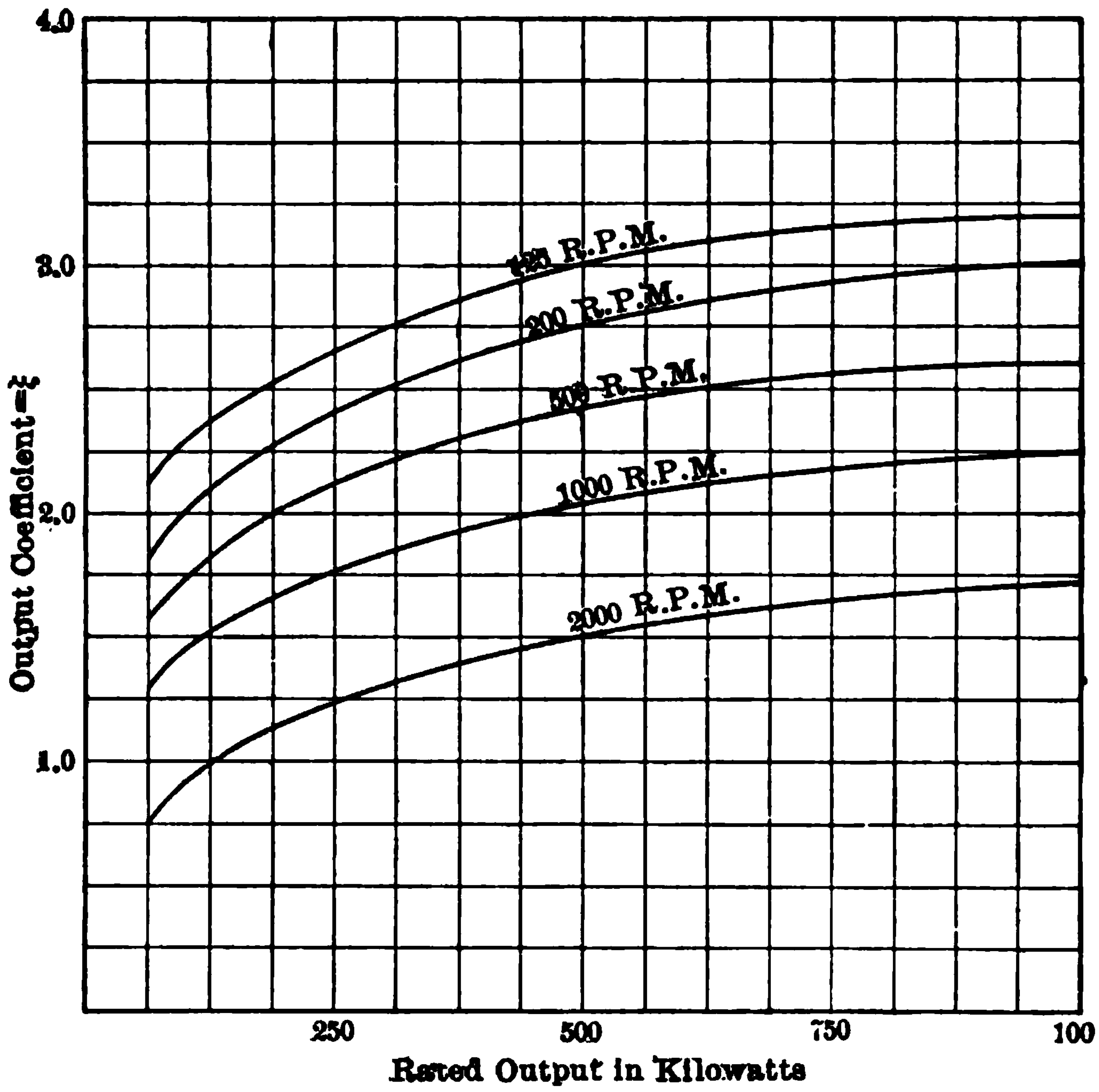


FIG. 9.— Output coefficient curves for 500 volts continuous current machines at different speeds and outputs.

It is desirable to point out in connection with the output coefficient that, while of great utility, it cannot be regarded as more than a nucleus from which to evolve a design. Employed with a clear understanding of this limitation, and with the reservation that its value must be adjusted according to the requirements of each individual design, the conception is of great utility to the designer.

In order to facilitate the intelligent use of the output coefficient,

we have set forth the data and curves of this chapter. While the values and relations given in the curves are fairly representative and show the average magnitude of the influence of the various factors involved, they should not be rigidly adhered to in cases where the individual design shapes out best by departing from these values.

Methods of designing alternating and continuous current machines, using the output coefficient ξ as a basis, are set forth in Chapters VII and XIV respectively.

We have seen how the specific electric and magnetic loadings, α and β, are related to the output coefficient ξ, viz. that ξ is proportional to the product $\alpha\beta$. The study of the value of ξ may be thus regarded as a study of the product $\alpha\beta$, although the influence of the speed and output on each of these factors individually has not as yet been ascertained, but will, for alternators, be dealt with in Chapters VIII, IX, and X.

In choosing a value for ξ as a starting point for a design, the designer is really fixing a value for the product $\alpha\beta$ without assigning individual values to them. This determines the value of $D^2\lambda g$, and the design may then be proceeded with in the manner outlined in Chapters VII and XIV.

In working out the further stages of the design the values of α and β will require to be determined, but it is often possible and desirable to vary considerably the values of α and β relatively to one another, without changing the originally chosen value of ξ. For instance, an alternator which requires especially close voltage regulation will generally have a low value for α but in many cases it may have the same value of ξ as a more normal machine.

We have now considered the coefficients (1) and (4) mentioned on page 6. The second coefficient mentioned there is $\frac{W}{D\lambda g}$, which has been known as the Steinmetz coefficient. This coefficient which we will denote by ψ is closely related to the output coefficient ξ.

We have

$$\xi = \frac{W}{D^2\lambda gR} = \frac{W}{D\lambda g} \times \frac{1}{DR} = \frac{\psi}{DR}.$$

Also the peripheral speed, S, in centimeters per minute, is equal to πDR.

Thus $$DR = \frac{S}{\pi},$$
and substituting this in the above equation, we have

$$\xi = \frac{\pi}{S} \times \psi,$$

and the relation between the two coefficients is established.

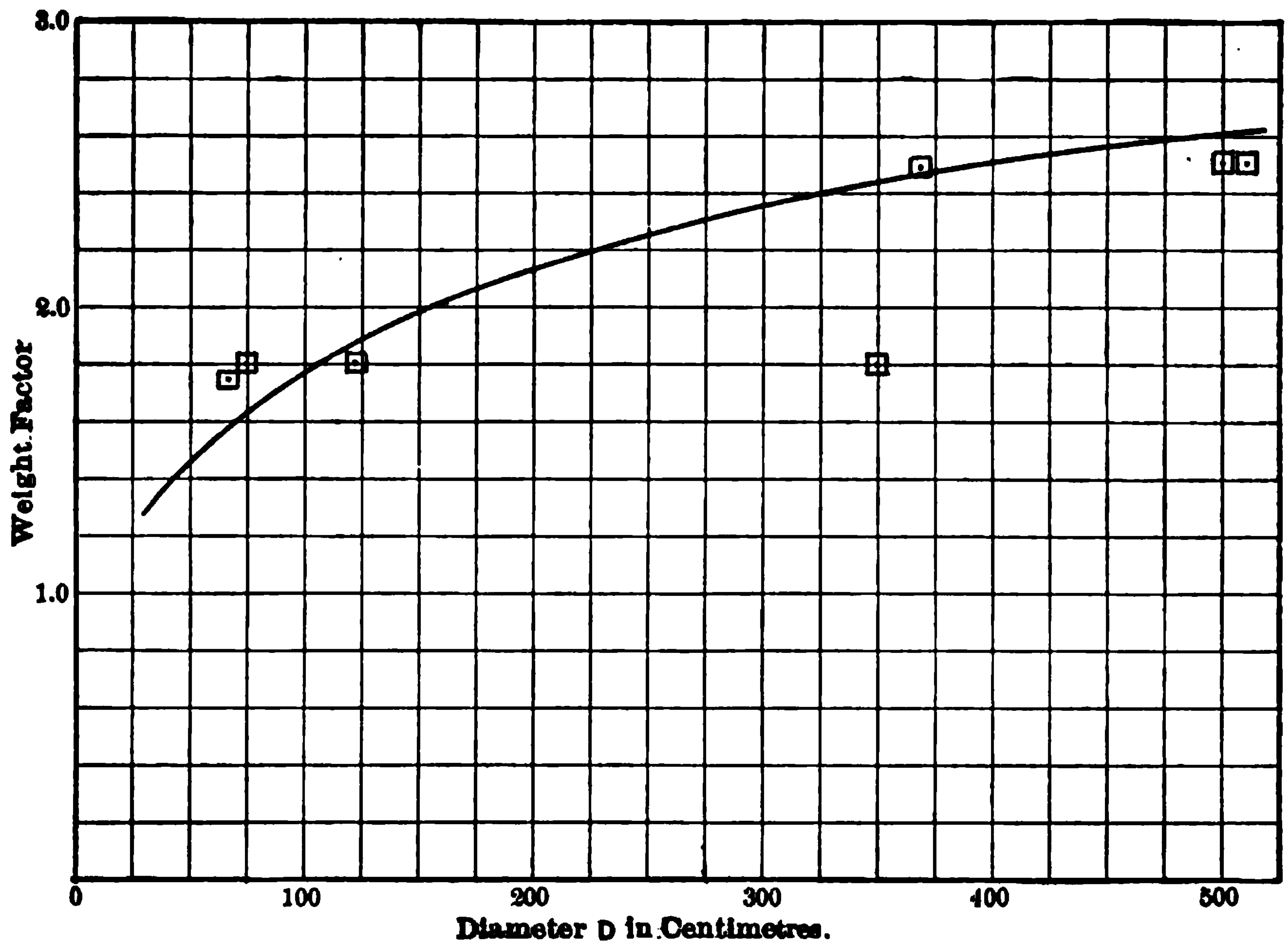


FIG. 10.— Weight factors of alternators.

B.

Weight Coefficients.

Alternators. — The total net weight of an alternating current generator may be roughly estimated either from the total weight of the effective material or from the air gap dimensions D and λg. The latter method may be based on a term proportional to the surface, as $D\lambda g$, or to the volume as $D^2\lambda g$.

The weight of the effective material, by which is meant the field iron (poles and yoke) and copper, and the armature iron (laminations) and copper, constitutes a fairly definite proportion of the

total weight of the machine, if there is not an abnormal amount of material put into the frame and spider.

We shall designate as the "weight factor" the ratio of the total

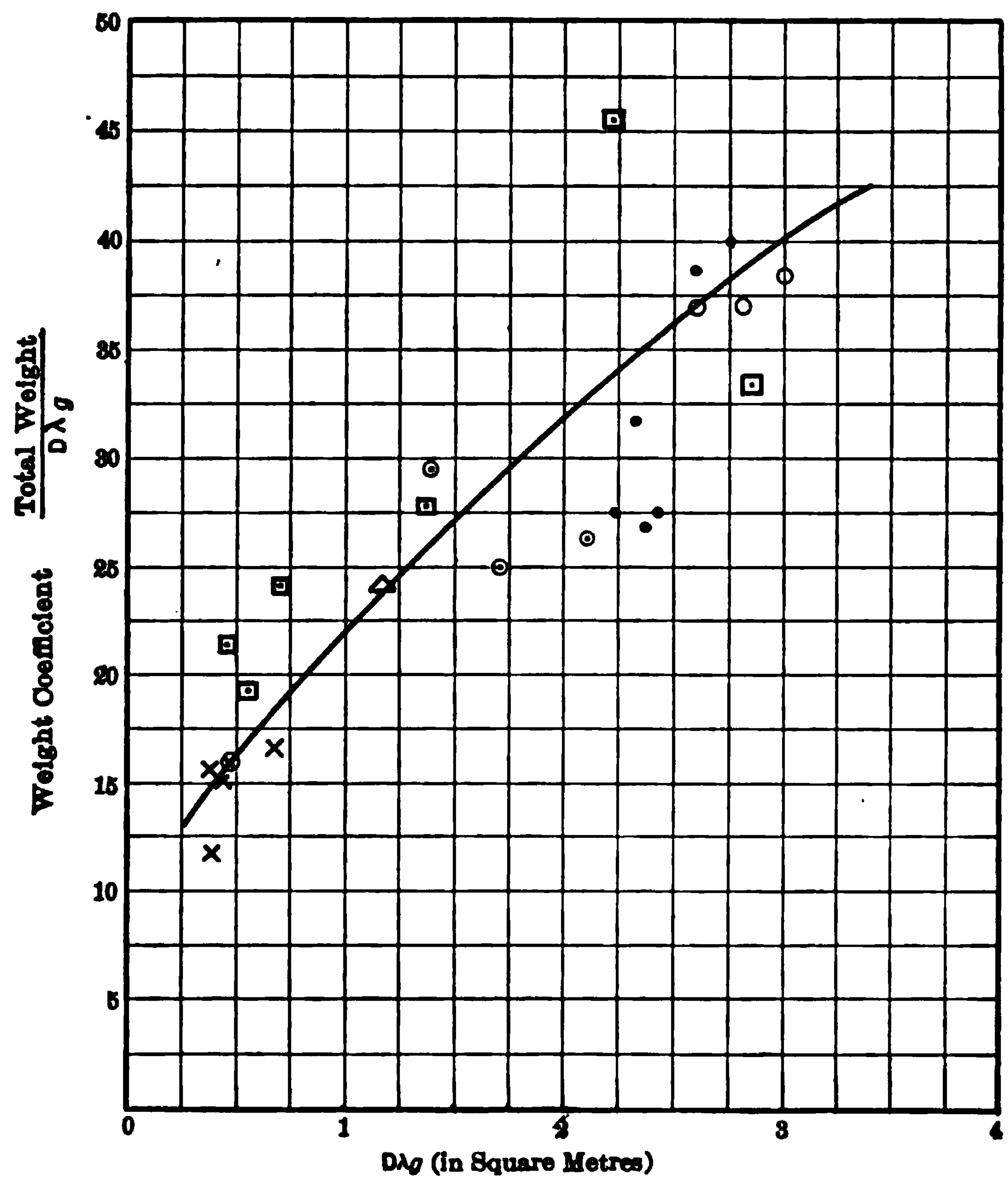


FIG. 11.— Weight coefficient— total weight ÷ $D\lambda g$ for alternators.

net weight (excluding bed plate and pedestals) to the weight of the effective material.

A curve showing rough but representative values for the weight factor for normal alternators in terms of the armature air gap diameter is given in Fig. 10. On the curve are marked the points corresponding to seven machines obtained from actual figures for the total weight.

The machines of small diameter have a lower factor than those

TABLE 1.

Weight Coefficients of Polyphase Alternators.

Reference No.	Rated Output in Kva.	Rated Speed in R.P.M.	Periodicity.	Diameter D in Cms.	Gross Core Length in λg Cms.	$D\lambda g$ in Meters.	$D^2\lambda g$ in Meters.	Total Weight of Effective Material in Tons.	Weight Factor.	Total Net Weight in Tons.	Effective Material Weight Coefficients. $\frac{EM}{D^2\lambda g}$	Effective Material Weight Coefficients. $\frac{EM}{D\lambda g}$	Total Weight Coefficients. $\frac{TW}{D^2\lambda g}$	Total Weight Coefficients. $\frac{TW}{D\lambda g}$	Distinguishing Symbol.
1	6000	750	25	200	130	2.6	5.2	48.1	2.0	96.0	9.25	18.5	18.5	37.0	○
2	6000	750	37.5	200	140	2.8	5.6	51.8	2.0	104.0	9.3	18.6	18.6	37.2	○
3	6000	750	50	200	150	3.0	6.0	57.4	2.0	115.0	9.6	19.2	19.2	38.4	○
4	3000	750	50	150	140	2.1	3.15	28.1	2.0	56.0	8.9	13.3	17.8	26.6	⊙
5	3000	1000	50	130	135	1.7	2.28	21.2	2.0	42.4	9.3	12.5	18.6	25.0	⊙
6	3000	1500	50	110	125	1.37	1.51	20.2	2.0	40.4	13.4	14.7	26.8	29.5	⊙
7	3000	83	25	500	55	2.75	13.75	38.7	2.8	110.0	2.8	14.0	8.0	40.0	●
8	3000	125	25	380	68	2.58	9.8	38.3	2.6	100.0	3.9	14.9	10.2	38.7	●
9	3000	250	25	230	100	2.30	5.3	30.0	2.4	72.0	5.7	13.0	13.6	31.3	●
10	3000	375	25	180	124	2.23	4.0	27.7	2.2	61.0	7.0	12.5	15.3	27.4	●
11	3000	500	25	150	155	2.33	3.5	31.3	2.0	63.0	9.0	13.5	18.0	27.0	●
12	3000	750	25	120	198	2.38	2.85	33.1	2.0	66.0	10.5	13.7	21.0	27.5	●
13	1500	750	50	135	85	1.15	1.55	15.4	1.8	28.0	10.0	13.5	18.0	24.0	△
14	650	1500	50	80	55	0.44	0.35	4.05	1.75	7.1	11.4	9.2	20.0	16.0	⊗
15	400	94	50	370	18	0.67	2.46	4.92	2.25	11.1	2.0	7.4	4.5	16.6	×
16	400	1500	50	75	47	0.35	0.25	3.14	1.75	5.5	12.5	9.0	22.0	15.7	×
17	400	3000	50	63.5	66	0.42	0.266	3.72	1.75	6.5	14.0	8.9	24.4	15.5	×
18	850	94	25	366	36.8	1.35	4.4	15.3	2.5	37.5	3.4	11.1	8.5	27.8	⊡
19	970	1500	...	75	74	0.55	0.415	5.65	1.8	10.2	13.7	10.4	24.6	18.5	⊡
20	1500	1000	50	122	58	0.71	0.87	9.22	1.8	16.5	10.5	13.0	19.0	23.3	⊡
21	2500	75	25	508	56	2.85	14.5	38.2	2.5	95.5	2.64	13.4	6.6	33.5	⊡
22	3000	75	...	688	68	4.68	32.2	61.0	2.0	122.0	1.9	13.0	3.8	26.0	⊡
23	3750	180	30	350	63.5	2.22	7.8	51.5	2.0	103.0	6.6	22.8	13.2	45.5	⊡

with large gap diameters. This is due to the larger amount of electromagnetically non-effective material in machines of large diameter, as in the arms of the magnet wheel, and in the stator frame.

Small diameters are associated with high speed machines, and thus the latter have generally a smaller weight factor and a smaller amount of non-effective material. The data relating to the points marked on the curve in Fig. 10 is given at the bottom of Table 1.

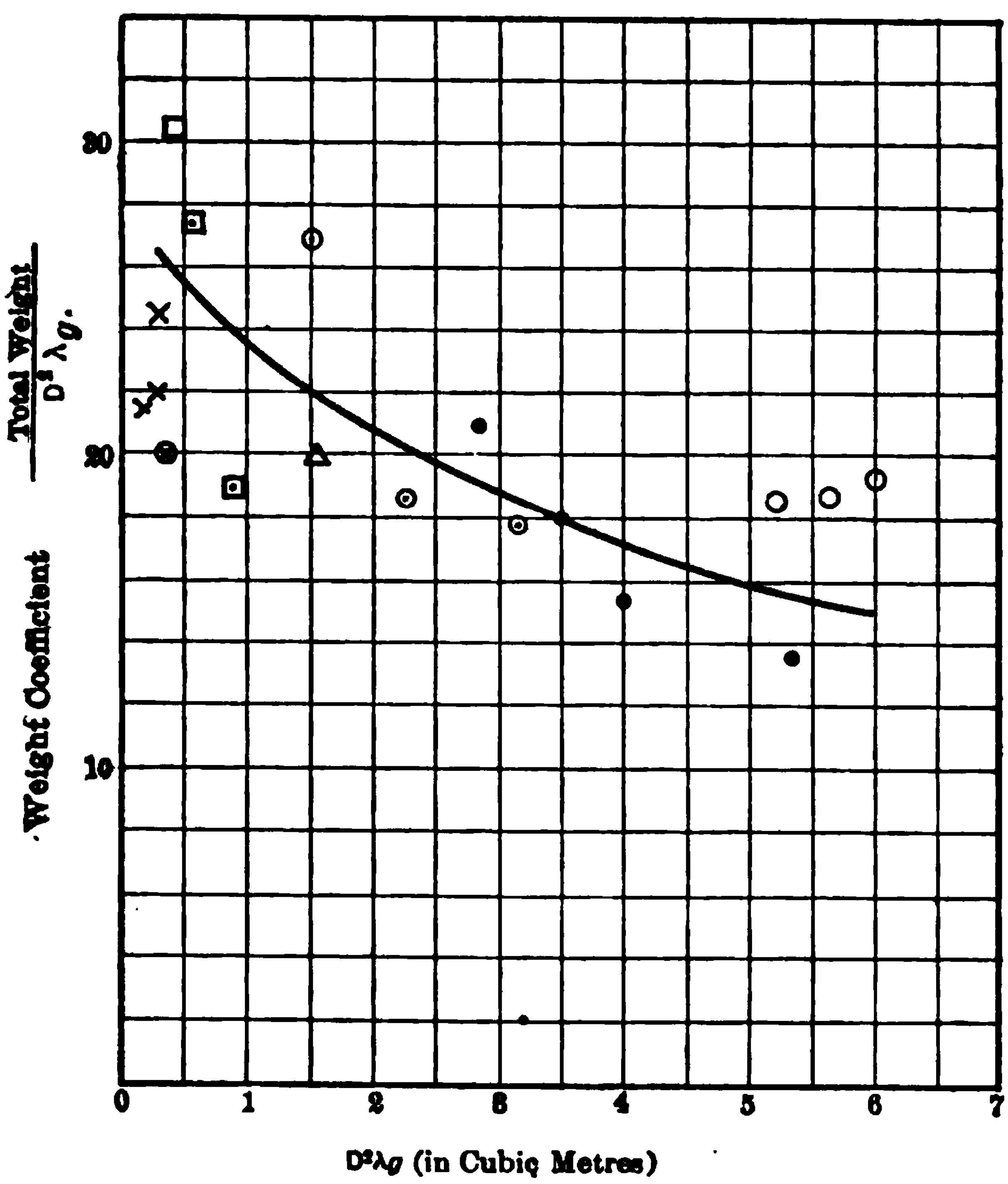


FIG. 12.— Weight coefficient — total weight ÷ $D^2\lambda g$ for high speed alternators.

The upper part of this table relates to the alternator designs for which specifications are given in Part II.

The total weights of effective material in column 9 are obtained from the detailed designs in Chapters VII to X.

The total net weights are then obtained from this by multiplying by appriopriate values of the weight factor taken from the curve in Fig. 10 and entered in column 10.

In columns 14 and 15 are calculated the weight coefficients for the total weights per unit of $D^2\lambda g$ and of $D\lambda g$, and in columns 12 and 13 the weights of effective material per unit of $D^2\lambda g$ and $D\lambda g$. The latter coefficients do not vary over so wide a range as do the total weight coefficients, as in the latter, the additional variable, the weight factor, is introduced.

In Fig. 11 is plotted the weight coefficient, (total weight ÷ $D\lambda g$), as a function of $D\lambda g$, the different styles of points making a dis-

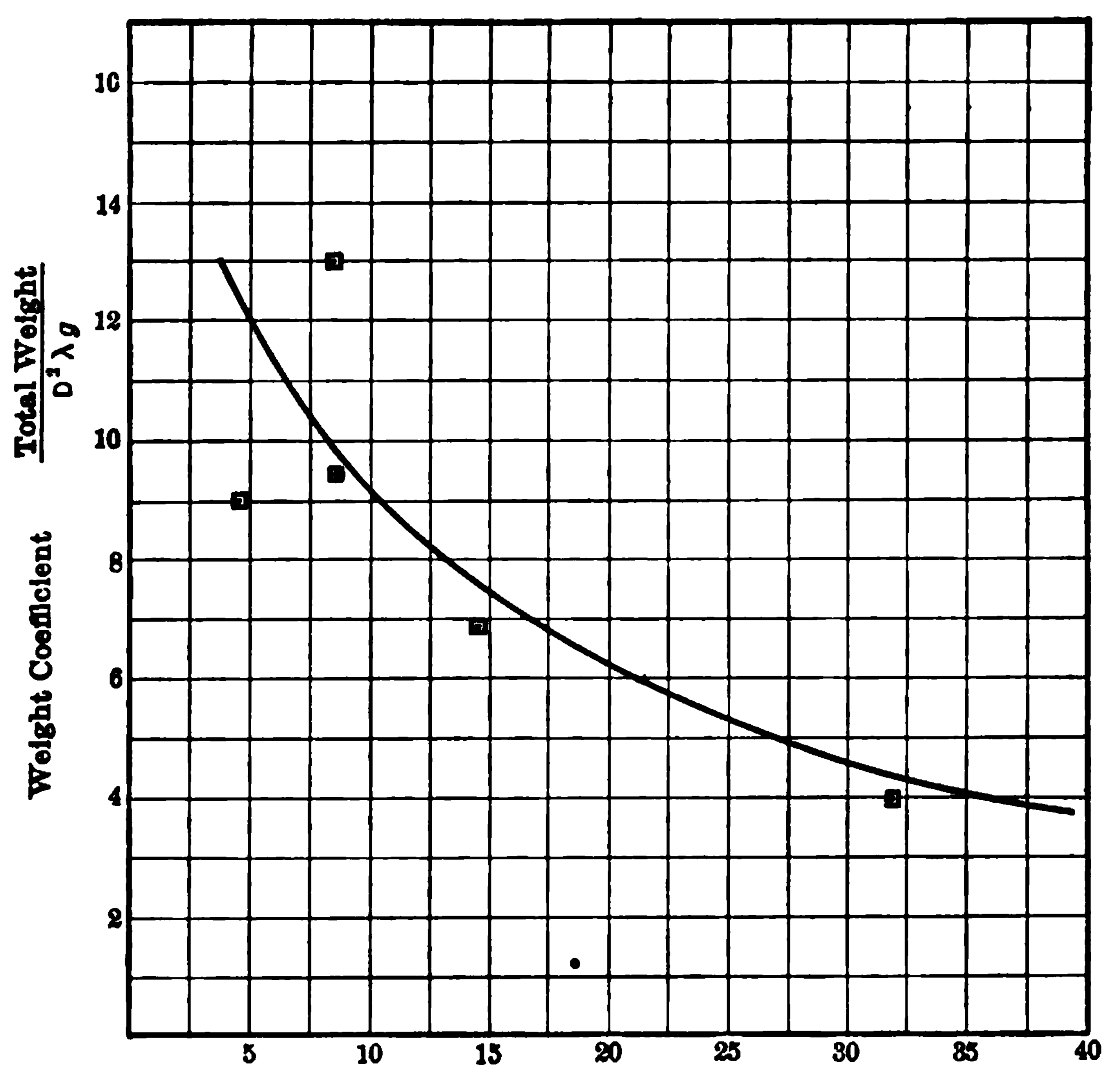


FIG. 13.— Weight coefficient— Total weight ÷ $D^2\lambda g$ for slow speed alternators.

tinction between the various designs. The points for the machines in the lower part of Table I are marked thus $\boxed{\cdot}$. This curve may be taken as suitable for both high and low speed machines, since the value of $D\lambda g$ is practically a constant for any given rated output and practically independent of the speed even in the largest ratings.

The value of $D^2\lambda g$, however, varies widely with the rated speed, for

a given rated output, as will be seen from the relation embodied in the output coefficient

$$\xi = \frac{W}{D^2 \lambda g R},$$

since ξ does not vary widely for a given rated output.

Hence in high speed machines, we are confined to a comparatively small range of values for $D^2\lambda g$, the maximum value being about 6 cubic metres. The range for slow speed machines extends

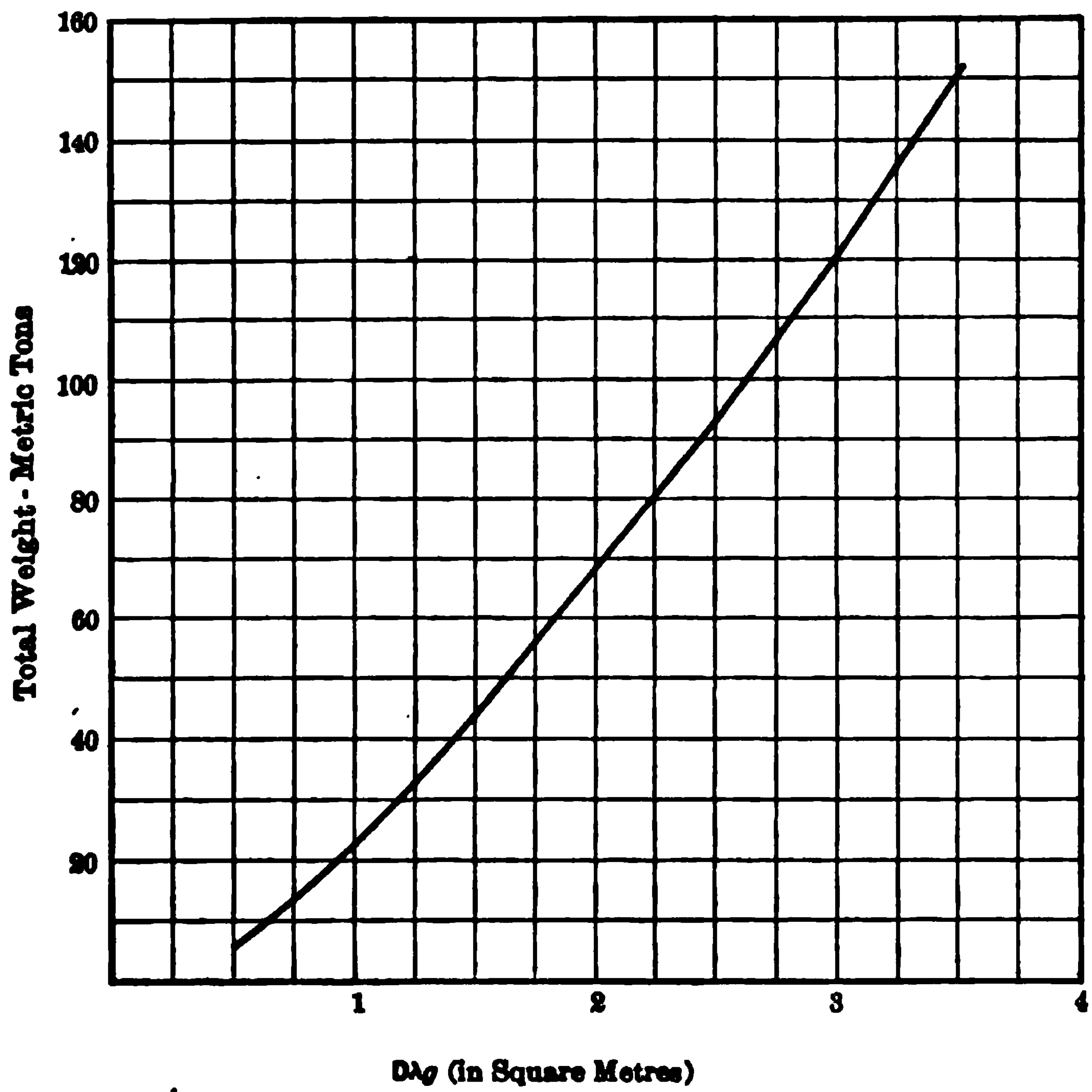


FIG. 14.— Relation between total weight of alternators and $D\lambda g$.

to as high as 40 cubic metres and hence we must take each class separately.

Fig. 12 shows the weight coefficients, (total weight ÷ $D^2\lambda g$) plotted against $D^2\lambda g$ for the high speed machines of Table I.

Fig. 13 gives a similar curve for slow speed machines obtained from an analysis of a large number of machines; by way of example the points corresponding to the machines in the lower half of Table I

have been entered. From the curves in Figs. 11, 12, and 13 we have derived, in Figs. 14, 15, and 16, curves showing the total net weight in terms of $D\lambda g$ and $D^2\lambda g$.

Continuous Current Machines. — For continuous current machines, a greater proportion of the total weight of material is effective, as there is no non-effective material corresponding to the armature frame in alternators, the magnet yoke of continuous current machines being self-supporting.

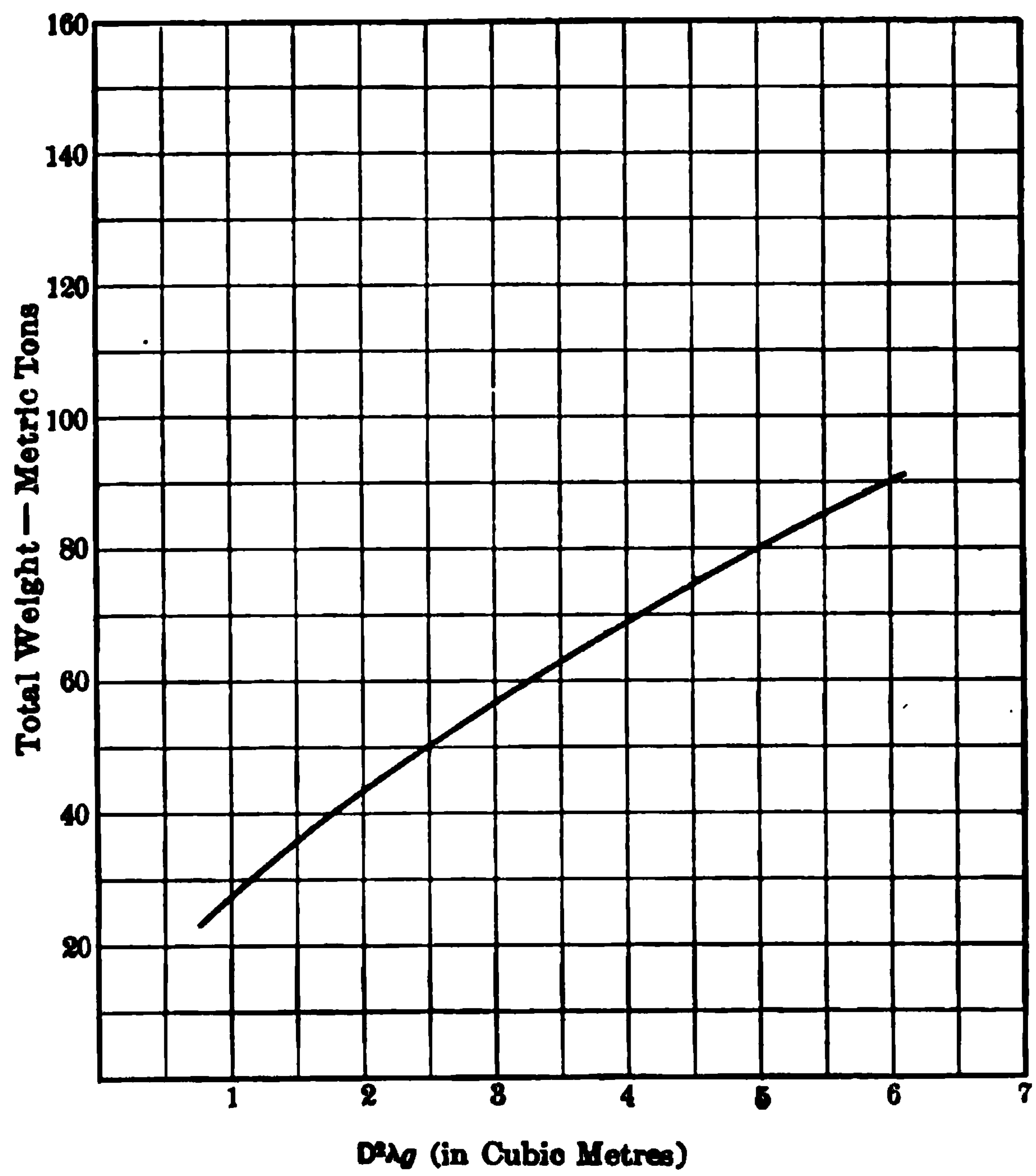


FIG. 15. — Relation between total weight and $D^2\lambda g$ for small slow speed alternators.

Hence the values of the weight factor, as defined above, are less in the case of continuous current machines than for alternators. The average values of the weight factor for continuous current machines usually range from 1.3 to 1.5, the higher values applying again to machines of large diameter.

In this case the effective material includes the weight of the commutator segments.

Fig. 17 shows the relation between the total weight of continuous

current machines and the value of $D^2\lambda g$. The curves are plotted from an analysis of a large number of machines. Curves are given for 125, 500, and 2000 R.P.M., which roughly indicate the influence of the speed on the relation of total weight to $D^2\lambda g$.

C.

Cost Coefficients.

The Total Works Cost of a machine may be roughly determined from the dimensions or weight, without recourse to detailed estimates of material and labour charges.

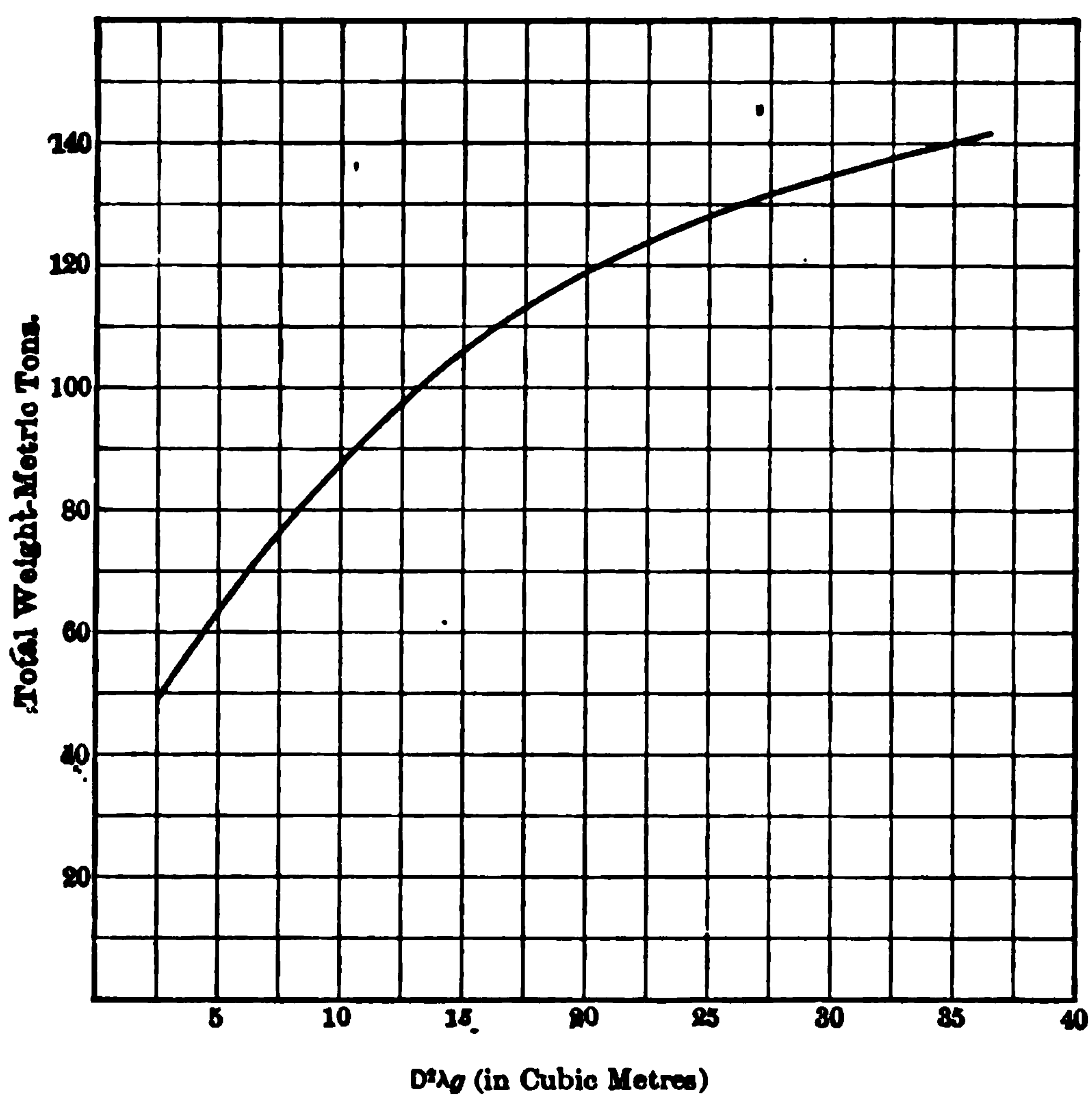


FIG. 16.— Relation between total weight and $D^2\lambda g$ for large slow speed alternators.

A method of estimating the Total Works Cost of continuous current machines from the air gap dimensions is given in Chapter XV. In this method the Total Works Cost is equal to

$$kDL \text{ or } kD\,(\lambda g + 0.7\,\tau),$$

where L is the length of the armature over the windings (which may be taken as approximately equal to $\lambda g + 0.7\,\tau$), and k is the cost

coefficient, the value of which varies, although not widely, with the rated output and the peripheral speed.

A useful rough method consists in working from the cost per ton of total weight of machine. The value of this figure for both alter-

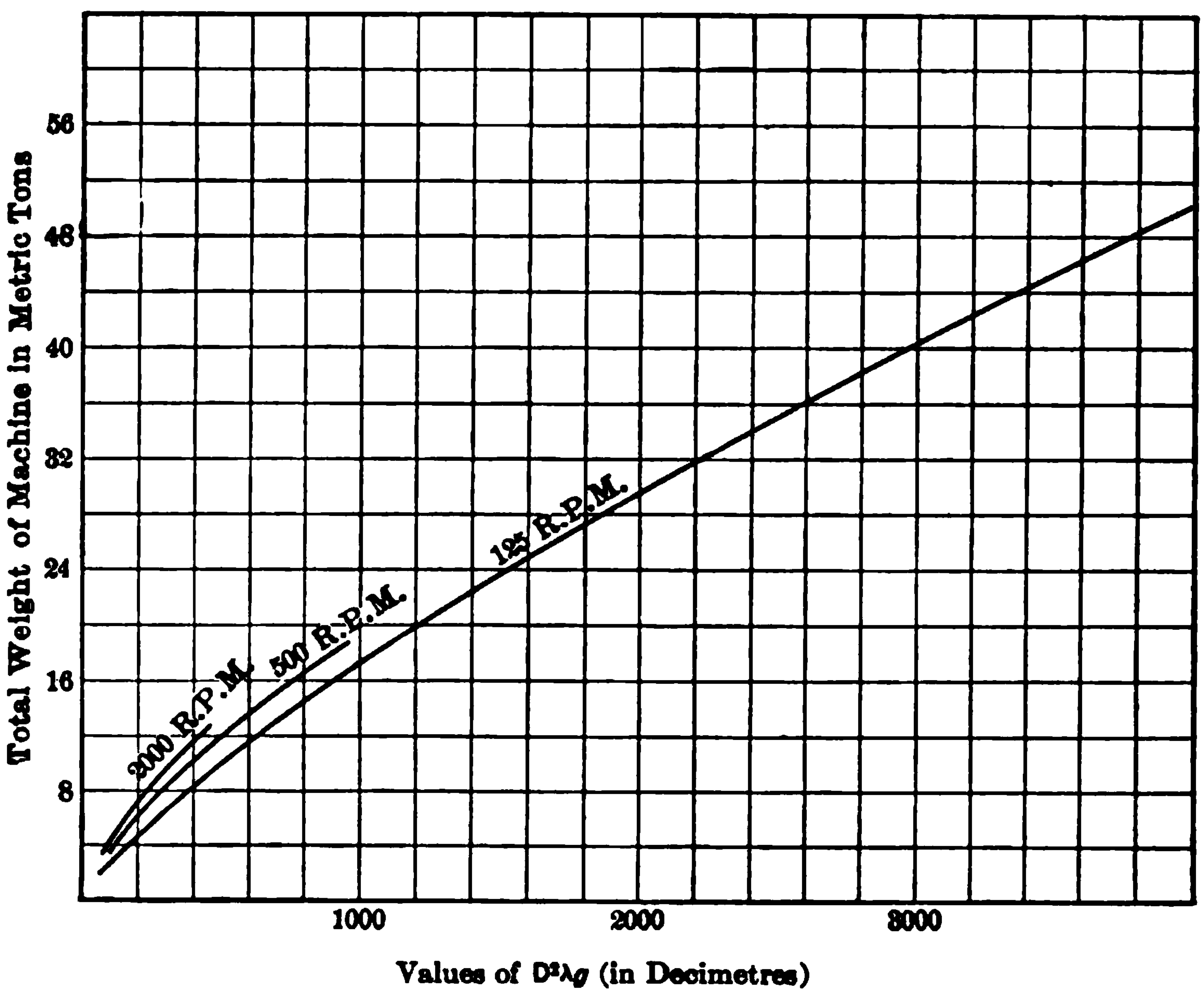


FIG. 17.— Relation of weight to $D^2\lambda g$ in continuous current machines at different speeds.

nating and continuous current machines of other than small rated outputs, averages from $150 to $200 per metric ton of total weight. The cost per ton is lower the greater the total weight.

CHAPTER III.

CRITERIA FOR HEATING AND FOR TEMPERATURE RISE.

THE ultimate temperature rise in a dynamo electric machine is proportional to the losses in the machine, and inversely to the radiating surfaces of the various parts of the machine in which the losses occur; i.e., the ultimate temperature rise is proportional to the watts lost per unit of radiating surface.

For the purposes of the present treatise, and in connection with the various designs worked out herein, we shall estimate the temperature rise on the basis of the watts dissipated *per square decimetre* of radiating surface.

Armature Losses and Temperature Rise of Armature.—The various losses throughout an armature are capable of being estimated to a fair degree of accuracy, but the true radiating surface of an armature, more especially in view of the fact that the losses occur in both the copper and the iron, is rather an indefinite quantity.

The iron surfaces at the sides of the ventilating ducts and the winding surfaces at the ends of the armature coils are all exposed to the cooling air, but they contribute to the radiation of the heat to widely different extents.

In the case of armatures of great core length the core ducts may receive air at a higher temperature than that at which it passes the windings, and it is possible for the ducts near the centre of the core to contribute very little to the heat dissipation. Furthermore, the velocity of the cooling air may vary considerably in different parts of the machine.

The desirability of estimating the heating, on the basis of surfaces common to all dynamo machines, will be readily appreciated.

We give here four methods, A, B, C, and D, which may be conveniently used for the purpose of estimating the temperature rise of armatures.

METHOD A.

A convenient basis for reference for armatures is the cylindrical surface at the air gap, extended to include the end connections of the windings. This surface comprises the armature core surface and the external surface of the end portions of the windings. Representing by D the armature diameter at the air gap, and by L the length over the ends of the windings, this surface is equal to $\pi \times D \times L$.

The overall length L for ordinary continuous current barrel-wound armatures is usually approximately equal to $(\lambda g + 0.7\tau)$ where λg denotes the armature gross core length and τ denotes the pole pitch. Of this length, 0.7τ is the part occupied by the end windings, which, according to this formula, project a distance equal to 0.35 of the pole pitch at each end. This is a very usual value in practice. The cylindrical surface over the winding is thus,

$$\pi \times D \times (\lambda g + 0.7\tau).$$

If D, λg, and τ are, for the purpose of these thermal calculations, taken in decimetres, the surface is in square decimetres, and this is the most convenient unit for expressing radiating surfaces. If the dimensions are taken in centimetres the surface must be divided by 100 to reduce it to square decimetres.

Denoting by W the total losses in watts throughout the armature (copper and iron), then the value of the watts per square decimetre of radiating surface as thus defined, is equal to

$$\frac{W}{\pi \times D \times (\lambda g + 0.7\,\tau)},$$

and for a given armature, running at a given speed, the temperature rise is roughly proportional to the value of this expression.

For ventilated revolving armatures of ordinary design, the thermometrically determined temperature rise lies between 0.6 deg. Cent. and 1.6 deg. Cent. per watt per square decimetre. The temperature rise per watt per square decimetre may be designated the "Specific Temperature Rise," and we may write,

$$T \text{ (in deg. Cent.)} = K \times \text{watts per square decimetre.}$$

The value of K, the "Specific Temperature Rise," is usually from 0.6 to 1.6. This value applies to ordinary armatures having a peri-

pheral speed of some 10 to 25 metres per second and to the thermometrically determined temperature rise.

For a peripheral speed of twice this, say 35 metres per second, the value of K will be from 0.5 to 1, provided there is an efficient flow of air through the armature core and windings.

In alternating current generators, it is more usual to have the armature winding on an external stator. This circumstance does not materially affect the value of K so long as the ventilating facilities are ample and the rotor peripheral speeds are of the stated values.

In turbine dynamos the peripheral speed generally reaches 50 to 80 metres per second, and may, in exceptional cases, be as high as 100 metres per second in large alternators. The watts per square decimetre for such machines is high by reason of the small dimensions required at high speeds, and a much smaller value of K must be attained by some means in order that the temperature rise shall not be abnormal. It is thus a matter of the greatest importance that the armatures of turbo-dynamos shall be provided with a very liberal number of ventilating ducts and that no expedient means of improving the ventilation shall be omitted.

With armatures of this class and for the range of peripheral speeds quoted above, i.e., 50 to 100 metres per second, the specific temperature rise may be more of the order of 0.3 to 0.6 deg. Cent. per watt per square decimetre.

We may tabulate the following values for K in the formula T (in deg. Cent.) $= K \times$ watts per square decimetre.

TABLE 2.

VALUES OF SPECIFIC (THERMOMETRICALLY DETERMINED) TEMPERATURE RISE IN TERMS OF PERIPHERAL SPEED OF ROTOR.

Average Peripheral Speed.	Value of K.
10 to 25 metres per second	0.6 to 1.6
35 metres per second	0.5 to 1.0
70 metres per second	0.3 to 0.6

These figures must be used judiciously and the individual circumstances of any particular case must be given careful consideration. While the data serves to give a good general idea, especially for purposes of comparison, due regard must be paid to each case, especially.

to the effectiveness of the ventilating arrangements. If the latter be well designed, the lower values of the specific rise may be taken. In the case of those turbo-dynamos in which the air is forced through the machine by means of fans and cups on the rotor, as is now frequently being done, the lower values will often be attainable, but of course any such calculations are best substantiated by test results on the class of machine being dealt with. There is frequently but very restricted space available for providing access of air to the interior of the rotor in the case of turbo-generators, and hence in spite of their high peripheral speeds they are in many cases at a grave disadvantage as compared with large slow speed generators where the active material of the rotor is supported at the rim of a large, open spider.

For alternating current armatures, where the winding is carried out as a barrel winding, similar to a continuous current winding, the above method and constants will hold good. The peripheral speed will generally be that of the rotor within the *stationary* armature.

If the winding of an alternator is carried out as a coil winding, as is commonly the case with stationary armatures, the "length over the windings" is indefinite, and consequently the cylindrical surface at the air gap is a much less definite conception. Nevertheless good results may be obtained even in these cases, by method A.

Various alternative methods are, however, available, and will now be briefly considered.

METHOD B.

One of these methods consists in using simply the armature core surface at the air gap, and the watts lost below that surface. This surface is simply $\pi D\lambda g$, and the watts requiring to be dissipated by this surface consist of the armature iron loss (core and teeth) plus the losses in the embedded portions of the conductors. This latter is equal to the total copper loss minus the loss in the end windings.

On this basis the specific temperature rise generally has values about two thirds as great as the values given in Table 2 which are intended for use with method A, in which the heating coefficient is calculated on the surface $\pi D(\lambda g + 0.7\tau)$.

Calculations by method B relate only to the heating of the armature body, the radiating surface of the end windings not being taken into account. As these are, however, when correctly designed, usually subject to better cooling facilities than the body of the arma-

ture, it may often be assumed that their temperature rise will not exceed that of the armature body, and calculation on the latter will suffice in such cases.

METHOD C.

Another alternative consists in employing as the "equivalent" cylindrical surface, the value of the expression

$$D \times (\lambda g + K\tau)$$

where K is a coefficient depending mainly on the rated voltage and also to a certain extent on the value of τ, the polar pitch. In Table 3 are given fairly representative values for K.

TABLE 3.

Values of K in the Expression $D \times (\lambda g + K\tau)$ Representing the Equivalent Cooling Surface of the Armature of an Alternating Current Generator.

Rated Terminal Voltage.	Values of K, when τ the Pole Pitch = 40 Cms. (or Less).	Values of K, when τ the Pole Pitch = 60 Cms. (or More).
1,000 or less	0.8	0.7
2,000	1.0	0.8
4,000	1.2	0.9
6,000	1.4	1.1
8,000	1.6	1.3
10,000	1.8	1.5
12,000	2.0	1.7
20,000	2.2	1.9

METHOD D.

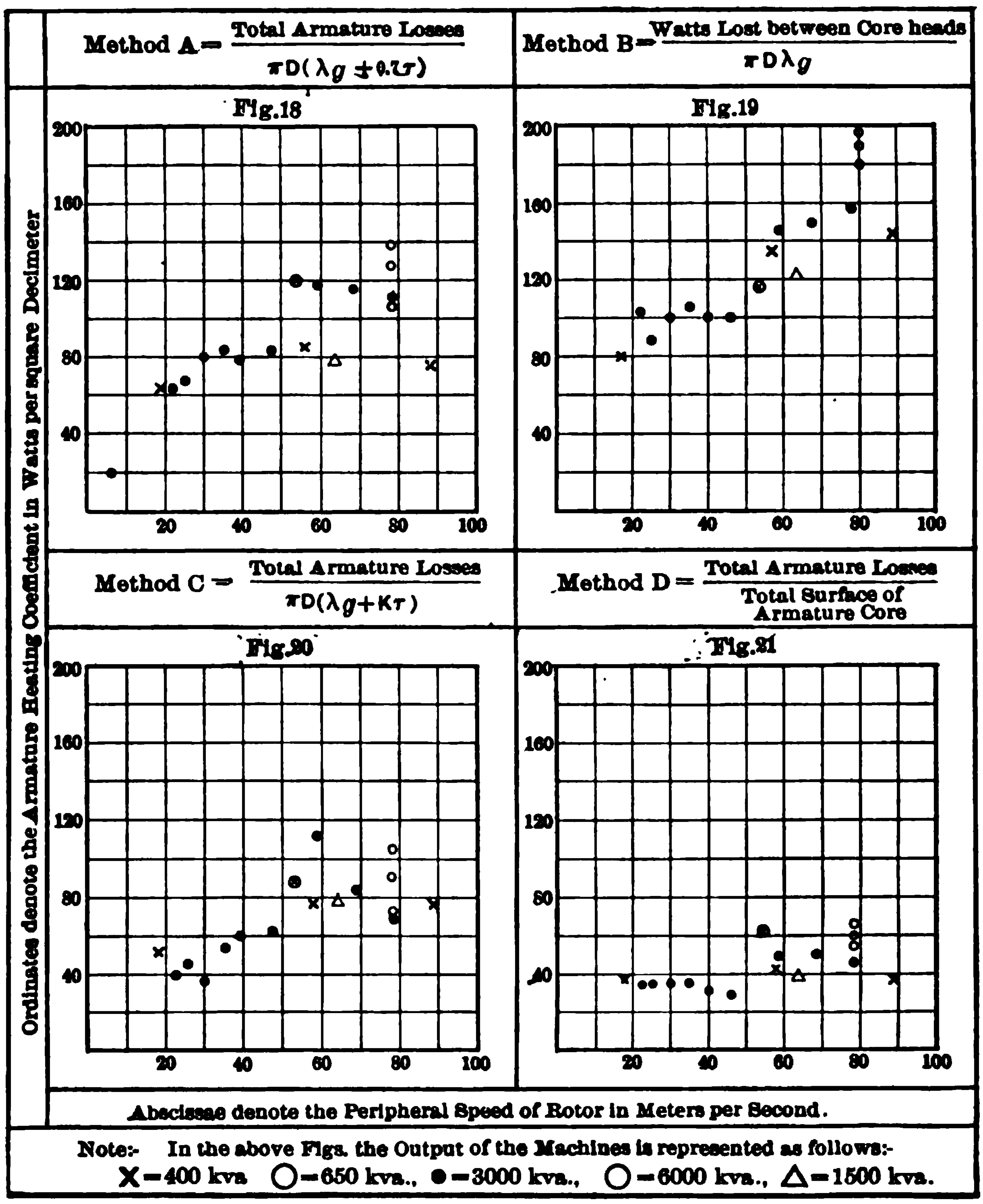
Another coefficient which may be used for estimating the armature heating, is the watts per square decimetre of total bounding surface, which (for stator armatures) is taken as consisting of the internal (air gap) and external cylindrical surfaces, and the surface of the two ends of the armature core.

This surface is equal to

$$\pi (D_1 + D)\, \lambda g + \frac{2 \times \pi (D_1 + D)}{2} \times \frac{(D_1 - D)}{2}$$

$$= \pi (D_1 + D)\, \lambda g + \frac{\pi}{2} (D_1^2 - D^2),$$

where D_1 and D are the armature external and internal diameters respectively, and λg is the gross core length.



FIGS. 18–21.—The armature heating coefficient for the alternating current generators specified in Chapters VII–X, calculated according to the four methods as set forth in Table 4.

In all of these methods we may denote as the heating coefficient, the quotient $\frac{\text{watts loss}}{\text{radiating surface}}$, i.e. the energy in watts requiring to be dissipated per square decimetre of radiating surface. Thus the (thermometrically determined) temperature rise is equal to the product of the "Specific Temperature Rise" and the "Heating Coefficient."

TABLE 4.

THE ARMATURE HEATING COEFFICIENTS FOR THE ALTERNATING CURRENT GENERATORS SPECIFIED IN CHAPTER VII-X BASED ON FOUR DIFFERENT METHODS OF CALCULATING THE RADIATING SURFACES.

Reference Number.	Rated Output in kva.	Rated Speed in Revolutions per Minute.	Periodicity in Cycles per Second.	Peripheral Speed of Rotor in Metres per Second.	Rated Voltage.	Pole Pitch (τ) in Centimetres.	Method A. Total Armature Loss in Kilowatts. w.	Method A. Radiating Surface (Sq. Dm.) $\pi D(\lambda g + 0.7\tau)$. a.	Method A. Heating Coefficient (Watts per Sq. Dm.). $\frac{1000w}{a}$.	Method B. Loss within Armature Core Heads in Kilowatts. w_1.	Method B. Radiating Surface (Sq. Dm.) $\pi D\lambda g$. b.	Method B. Heating Coefficient (Watts per Sq. Dm.). $\frac{1000w_1}{a}$.	Method C. Value of K in Expression $\pi D(\lambda g + K\tau)$, see Table 3.	Method C. Radiating Surface (Sq. Dm.) $\pi D(\lambda g + K\tau)$. c.	Method C. Heating Coefficient (Watts per Sq. Dm.). $\frac{1000w}{c}$.	Method D. Total Radiating Surface of Armature Core (Sq. Dm.). d.	Method D. Heating Coefficient (Watts per Sq. Dm.). $\frac{1000w}{d}$.	Designating Symbol for Curves in Figs. 18-21.
I		750	25	78.6	11,000	157	160.5	1510	106	147	820	180	1.6	2400	66	2760	58	○
II	6000	750	37.5	78.6	11,000	105	176.5	1340	131	167	880	190	1.6	1910	92	2985	60	○
III		750	50	78.6	11,000	78.6	181	1300	139	174	940	195	1.6	1730	105	3150	57.5	○
IV		750	50	59	11,000	59	99.4	850	117	95	660	144	1.6	1110	112	2050	48	●
V	3000	1000	50	68	11,000	68	86.4	750	115	83	550	150	1.6	1000	86	1720	50	●
VI		1500	50	86	11,000	86	72.2	640	113	68	430	158	1.6	910	79	1590	45.5	●
VII		83	25	22	11,000	44	85.9	1350	65	68	660	103	1.9	2170	40	2330	37	●
VIII		125	25	25	11,000	50	83.4	1230	68	70	810	87	1.8	1890	44	2175	38	●
IX	3000	250	25	30	11,000	60	81	1030	80	72	720	100	1.6	2140	38	2100	39	●
X		375	25	35	11,000	70	82.4	980	84	73	700	104	1.6	1460	56	2145	38	●
XI		500	25	39	11,000	78	80	965	78	73	730	100	1.6	1320	61	2435	33	●
XII		750	25	47	11,000	94	82.6	990	83	76	750	101	1.6	1310	63	2605	32	●
XIII	1500	750	50	53	11,000	53	62.6	520	120	59	360	114	1.7	740	85	1000	62	△
XIV	650	1500	50	63	500	63	20	250	80	17	138	123	0.7	250	80	470	42.5	⊗
XV		94	50	18.2	3,000	18.2	23	360	64	17	210	81	1.1	440	52	618	37.0	×
XVI	400	1500	50	59	3,000	59	18	210	86	15	110	135	0.9	235	77	424	42.5	×
XVII		3000	50	89	550	89	20	255	78	19	132	143	0.7	255	78	509	39	×
For Graphical Results see figure number									18			19			20		21	

In the diagrams in Figs. 18 to 21 we have for a number of the authors' alternator designs, particulars of which are given in subsequent chapters, plotted the heating coefficients as a function of the peripheral speeds of the rotor.

TABLE 5.

THE ARMATURE HEATING COEFFICIENTS FOR TWENTY-NINE ALTERNATING CURRENT GENERATORS BASED ON THREE DIFFERENT METHODS OF CALCULATING THE RADIATING SURFACE.

Peripheral Speed of Rotor in Metres per Second.	Rated Pressure in Volts.	Pole Pitch τ.	Method A.			Method B.		Method C.		
			Approximate Total Armature Loss in Kilowatts	Radiating Surface (Sq. Dcm.) $\pi D (\lambda g + 0.7 \tau)$.	Heating Coefficient. Watts per Sq. Dcm.	Radiating Surface (Sq. Dm.) $\pi D \lambda g$.	Heating Coefficient Watts per Sq. Dm.*	Value of K in Expression $\pi D (\lambda g + K\tau)$, see Table 3.	Radiating Surface (Sq. Dm.) $\pi D (\lambda g + K\tau)$	Heating Coefficient Watts per Sq. Dm.
			w.	a.	$\frac{1000w}{a}$	b.	$\frac{1000w}{b}$			$\frac{1000w}{c}$
20.4	530	20.4	5	73.5	65	45	106	0.8	78	64
38	2,400	20.8	6	131	46	90	67	1.0	152	39.4
42	2,200	28	14	314	44	[illegible]	74	1 0	365	38.4
81	2,080	78.5	14	187	73	100	138	0.8	200	70
28.3	3,600	28.3	16	170	94	113	142	1.2	210	76
72	...	72	16.5	163	100	91	180	...	...	...
67.5	..	33.8	18	204	90	173	106	..	...	...
36.4	3,600	36.4	20	...	..	610	..	1.2	800	25
113	2,000	94	20	257	78	132	150	0 8	275	73
89	550	69	25	230	109	117	214	0 7	226	110
89	550	51.3	25	376	67	247	100	0.7	380	66
82.5	...	82.5	45	356	128	165	273	...	...	...
71	5,200	71	45	430	105	294	153	1.1	506	89
67.5	2,000	67.5	51	356	143	230	220	0.8	375	136
42	...	42	60	504	119	350	170	..	...	...
61	.	61	64	360	177	200	320	...	...	...
64	11,000	64	64	396	161	224	286	1.6	620	103
100	8,000	100	76	600	126	324	234	1.3	850	90
64	11,000	64	79	396	200	224	350	1.6	620	127
58	6,000	58	80	875	91	695	115	1.1	[illegible]	81
44.5	11,000	53	97.5	875	111	560	174	1 7	1320	74
86.5	5,000	108	122	980	125	650	188	1.2	1200	102
42.5	2,200	88	131	1300	100	645	203	0.8	[illegible]	95
39	3,500	39	131	806	164	585	224	1.2	950	138
57.5	..	115	150	1350	111	830	180	...	...	...
62	9,000	125	150	1600	94	960	156	1.4	1500	100
90	...	134	165	1300	127	790	210	...	...	..
50	12,000	100	215	2380	90	1550	140	1 7	3550	60
50	12,000	100	215	2380	90	1550	140	1.7	3550	60

In this table the heating coefficients (B) are based on the approximate total losses in the armature, no deduction being made for the losses in the end windings as in Table 4.

The data from which these curves have been plotted are set forth in Table 4.

In this table are given the leading particulars of these machines, and in the column marked w are given, in kilowatts, the total electrical losses in the armature. Column a represents the radiating surface calculated from the expresssion $D\ (\lambda\, g + 0.7\tau)$.

The corresponding heating coefficient is marked $\frac{1000\ w}{a}$ and the results are plotted in Fig. 18, also marked A. Columns b and c in Table 4 similarly represent the radiating surfaces $\pi D \lambda g$, and $\pi D\ (\lambda\, g + K\tau)$ and the results are plotted in Figs. 19 and 20 respectively. Column d in the same table refers to the total radiating surface of the armature core, including the end surfaces as well as the internal and external cylindrical surfaces. The results are plotted in Fig. 21.

Published data of other designers' machines is too meagre to permit of corresponding analyses. With an endeavour, however, to take such designs into consideration, Table 5 has been prepared.

In this table are calculated the rough approximate values above referred to, for three of the heating coefficients, for the twenty-nine alternators of which further data is given in Table 16, on p. 98.

In these designs a very rough approximation to the total armature losses was obtained from the assumptions contained in Table 6.

TABLE 6.

MEAN VALUES FOR ARMATURE LOSSES IN ALTERNATORS, IN PER CENT OF RATED OUTPUT.

Rated Output kw.	Total Armature Losses in Per Cent of Rated Output.
Up to 500 kw	5.0%
500 to 1000 kw.	4.5%
1000 to 2000 kw.	4.0%
2000 to 4000 kw.	3.5%
4000 kw. and over	3.0%

While weight must not be attached to any individual value in Table 5, the matter of interest is the order of magnitude of the coefficients. Neglecting those values which are unduly high or low, the general order of magnitude of the figures is fairly uniform, when taken in conjunction with the corresponding outputs and peripheral speeds.

In slow speed designs, where the losses are more concentrated in the "active belt," i.e., at the air gap periphery of the armature, the watts per square decimetre of that surface is the more rational coefficient to take out. But in high speed machines, where owing to the small number of poles, the armatures are of great radial depth, the bulk of the losses occur in the armature iron, and the watts per square decimetre of the total external armature surface becomes a more real conception.

Of course none of these coefficients are true thermal constants, as no account is taken in any of them of the exposed surfaces in the ventilating ducts. A method which takes account of the ventilating ducts is suggested later in this chapter on page 38.

In certain types of machines, it is very useful to calculate still another heating coefficient; that is, the total losses in both rotor and stator, divided by the air gap surface, as calculated by any of the foregoing methods; that is, we can divide the *total* electrical losses by any of the following surfaces:

(A) $\pi D(\lambda g + 0.7\tau)$

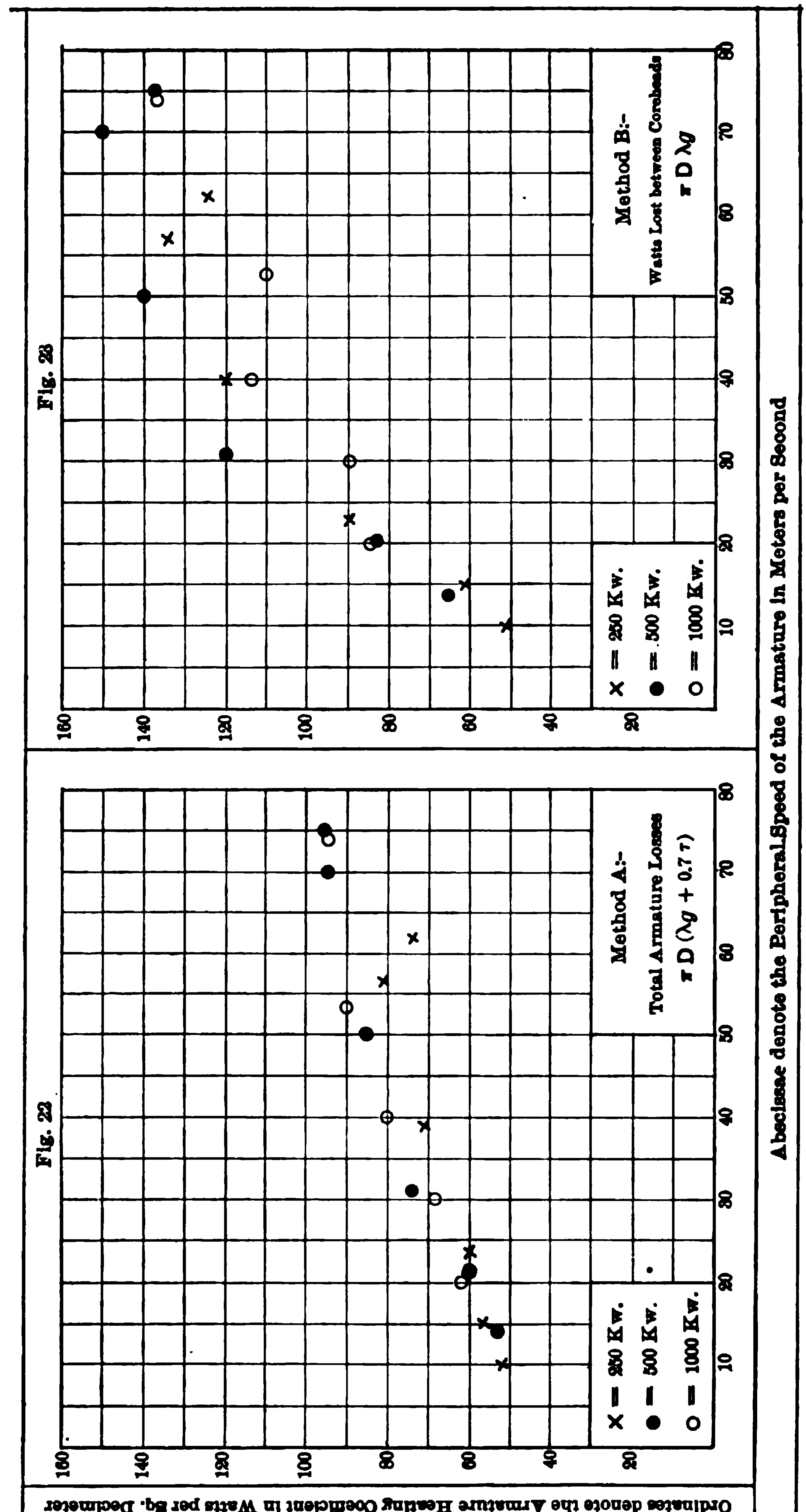
(B) $\pi D\lambda g$

(C) $\pi D(\lambda g + K\tau)$.

Machines for which this course is to be especially recommended, are those in which both stator and rotor are provided with distributed windings. This applies at once to induction motors and to turbo-alternators having a smooth cylindrical rotating field construction with distributed field windings.

Continuous Current Machines. — From the collection of preliminary continuous current designs which the authors have worked out and tabulated in Chapter XV, the armature heating data of Table 7 has been compiled. The curves in Figs. 22 and 23 correspond to this data.

For continuous current machines, the most rational heating coefficients are employed in Methods A and B. As in the case of the alternators, coefficient A is obtained by dividing the total armature losses by the cylindrical air gap surface taken over the ends of the windings, and coefficient B represents the watts lost between the armature core heads, divided by the cylindrical air gap surface, also taken between the core heads. For both these methods, the units



FIGS. 22 and 23.—The armature heating coefficients for the continuous current generators specified in Chapter XV, calculated according to the two methods set forth in Table 7.

are, as before, chosen to give the resulting heating coefficient in watts per square decimetre.

With regard to the remaining methods, C has no application to continuous current machines, as present practice is not concerned with the high voltages obtaining in alternators. It is sufficient in all cases, to take K equal to 0.7. Consequently Method C becomes identical with Method A.

Method D is generally unsuitable for application to high speed continuous current machines, owing to the fact that the internal cylindrical surface of the laminations very seldom contributes appreciably to the radiating surface of the armature.

The laminations are often mounted directly upon the shaft, and longitudinal tunnels stamped in the laminations provide a path for the air and link up with the ducts between the laminations.

TABLE 7.

THE ARMATURE HEATING COEFFICIENTS FOR THE CONTINUOUS CURRENT GENERATORS SPECIFIED IN CHAPTER XV. CALCULATED ACCORDING TO THE TWO METHODS A AND B.

Rated Output in Kilowatts.	Rated Speed in Revs. per Minute.	Frequency in Cycles per Second.	Armature Peripheral Speed in Metres per Second.	Method A.			Method B.			Designating Symbol for Curves in Figs. 22 and 23.
				Total Armature Loss in Kilowatts.	Radiating Surface $\pi D(\lambda g + 0.7\tau)$.	Heating Coefficient, (Watts per Sq. Dm.).	Loss within Armature Core-heads in Kilowatts.	Radiating Surface (Sq. Dm.) $\pi\lambda g$.	Heating Coefficient, (Watts per Sq. Dm.).	
				w.	a.	$\frac{1000w}{a}$.	w_1.	b.	$\frac{1000w_1}{b}$.	
250	125	10.5	10	14.4	280	51	6.8	132	52	×
	250	16.8	15	11.2	200	56	6.4	104	62	×
	500	25.0	23	9.6	160	60	6.3	70	90	×
	1000	50.0	40	8.9	125	72	6.0	50	120	×
	2000	66.6	57	7.4	90	82	5.4	40	135	×
	3000	50.0	62	6.9	93	74	4.9	39	125	×
500	125	14.7	14	22.8	430	53	13.9	210	66	●
	250	21.0	21	19.5	320	60	12.2	150	81	●
	500	33.3	31	17.7	240	74	12.3	112	120	●
	1000	50.0	50	16.3	190	86	12.7	90	140	●
	2000	66.6	70	15.3	160	95	12.8	79	150	●
	2500	83.0	75	13.65	140	96	12.4	90	138	●
1000	125	16.7	20	42.5	700	61	26.4	320	83	⊙
	250	25.0	30	37.0	550	68	20.9	230	90	⊙
	500	33.3	40	31.5	390	80	21.6	190	114	⊙
	1000	50.0	53	24.6	270	91	17.5	158	110	⊙
	2000	66.6	74	24.7	260	95	22.8	165	138	⊙
For Graphic Results see figure						22			23	

Influence of the Rotor Core Ducts.—All the preceding data is necessarily crude in that no direct account is taken of the proportions and number of ventilating ducts. Obviously there should enter into the formulæ, terms related to the rate of flow of air through these ducts, and to the extent and nature of the surfaces swept by this air. One of the authors' assistants, Mr. E. Coad, has, at their request, worked out the following method, which involves a quantity which may be designated the "Ventilation Coefficient" of the machine. The method as here described is planned, in the first instance, for the stator armatures.

To Determine the Ventilation Coefficient of a Machine.

Let n = the number of ducts.
a = the width of each duct in centimetres.
H = the number of slots.
t = minimum width of tooth in centimetres.

The area for the passage of air through the ducts = $nwHt$ square centimetres. Since the volume of air which passes through the ducts is proportional to the peripheral speed, the volume of air which passes through the ducts per second will be proportional to:

$$S \times n \times a \times H \times t,$$

where S = the peripheral speed of the rotor in metres per second.
Let D_1 = the external diameter of the armature in centimetres.
D = the air gap diameter in centimetres.

Then the area of one side of one armature lamination

$$= \frac{\pi}{4} (D_1^2 - D^2) \text{ square centimetres.}$$

Therefore the total area of duct surface exposed to the air passing through the ducts is equal to

$$2 \times n \times \frac{\pi}{4} (D_1^2 - D^2) = \frac{n \times \pi}{2} (D_1^2 - D^2) \text{ square centimetres.}$$

Assuming that a unit volume of air carries away with it a definite quantity of heat, whatever its velocity, the temperature rise will be inversely porportional to the volume of air passing per second through the ducts. The temperature rise is also inversely proportional to the surface exposed to the air. Therefore if we multiply the volume of air per second by the exposed surface we obtain a coefficient to which the temperature rise is inversely proportional.

This coefficient, which may be termed the ventilation coefficient, combines the two chief components tending to promote ventilation, and is numerically equal to

$$\frac{n \times \pi}{2}(D_1^2 - D^2) \times S \times n \times a \times H \times t$$

$$= \frac{\pi}{2}(D_1^2 - D^2) \times S \times a \times H \times t \times n^2.$$

As an example of the method of calculating and using the ventilation coefficient, let us consider the case of a 970 kva. alternator.

The following are the chief dimensions (in centimetres) with which we are concerned in the present investigation.

Rated output — kva.	970
Number of phases	3
Number of poles	4
Revolutions per minute	1500
Periodicity — cycles per second	50
Rated voltage	450
External diameter of armature laminations (D_1)	128
Diameter of armature at air gap (D)	75
Gross length of armature core (λg)	74
Number of ventilating ducts (n)	9
Width of each duct (a)	1.0
Net length of armature core (λn)	58
Ratio of $\frac{\lambda n}{\lambda g}$	0.78
Number of slots (H)	60
Dimensions of slots	2.3 dia.*
Slot pitch	3.93
Width of tooth (t)	1.63
Peripheral speed of rotor in metres per second (S)	60

Following the method of procedure outlined above, the smallest area for the passage of air through the ventilating ducts is equal to $naHt = 9 \times 1 \times 60 \times 1.63 = 880$ square centimetres. The peripheral speed (S) is equal to 60 metres per second, and the total cooling surface provided by the ducts is equal to

$$\frac{n\pi}{2}(D_1^2 - D^2) = \frac{9\,\pi}{2}(128^2 - 75^2) = 152{,}000 \text{ square centimetres.}$$

Hence the ventilating coefficient is equal to

$$880 \times 60 \times 152{,}000 = 8 \times 10^9.$$

* The armature slots of this machine are of circular section and semi-enclosed.

This quantity 8×10^9 is proportional to the amount of heat that is dissipated by the passage of air through the ventilating ducts. The armature losses in this machine are equal to 29,600 watts, and the resulting temperature rise was ascertained to be 53 deg. Cent. on the armature iron, 50 deg. Cent. on the armature winding, and 43 deg. Cent. on the magnet winding. All these are thermometrically determined. The radiating surface of the armature core, calculated according to method D, is equal to 640 square decimetres. The heating coefficient is equal to $\frac{29,600}{640}$ or 46 watts per square decimetre. This is equivalent to $\frac{46}{53}$, or 0.88 watts per square decimetre per deg. Cent. A stationary piece of iron that is not ventilated will dissipate only about 0.15 watt per square decimetre per deg. Cent.; we must conclude therefore that the remainder constituting 83 per cent of the total heat is dissipated through the direct agency of the circulating air.

In this machine ($\frac{0.15}{0.88} \times 29,600$) or 5000 watts would be dissipated by the action of radiation alone, and the fact that the remaining 24,600 watts are dissipated, for the same temperature rise, is due to the efficacy of the ventilation scheme. There should therefore be some direct connection between the amount of this extra heat and the ventilating coefficient 8×10^9.

For a first approximation, it may be said that a ventilating coefficient of one million would correspond to a dissipation of about $\frac{24,600}{8000}$, or, say, 3 watts. A large number of actual machines should be analysed before placing much reliance on this figure.

Field Coils. — For an ordinary field coil, when stationary, and in the presence of a stationary armature, the thermometrically determined specific temperature rise is from 4 to 5 deg. Cent.* per watt per square decimetre taken on the basis of the external exposed cylindrical surface of the coil.

If the coil is in the presence of a rotating armature, this figure may,

* Goldschmidt (*Journ. I. E. E.* vol. 34, p. 660) gives 15 deg. Cent. per watt per square decimetre, but this is based on the whole surface of the coil (internal and external) for medium sized semi-enclosed machines, and the rise is measured by resistance increase, which is often some 50 to 70 per cent greater than that observed by the thermometer. Dr. S. P. Thompson, in the discussion on this paper, gave a figure of 3.5 deg. Cent. due to Esson, 4.2 deg. Cent. due to the Oerlikon Co., and 7.1 deg. Cent. due to Neu and Levine, the latter being by resistance measurement, and all being reckoned on the *external* cylindrical surface of the coil. See also Rayner, "Report on Temperature Measurements at the National Physical Laboratory," *Journ. I. E. E.*, vol. 34, p. 613.

for peripheral speeds above 17 metres per second, be reduced to the extent of some 25 per cent to 50 per cent, the smaller figures applying to cases of small winding depth and well ventilated coils and high speeds.

The general magnitude of the effect of the peripheral speed of the armature on the temperature rise of field coils is very clearly brought

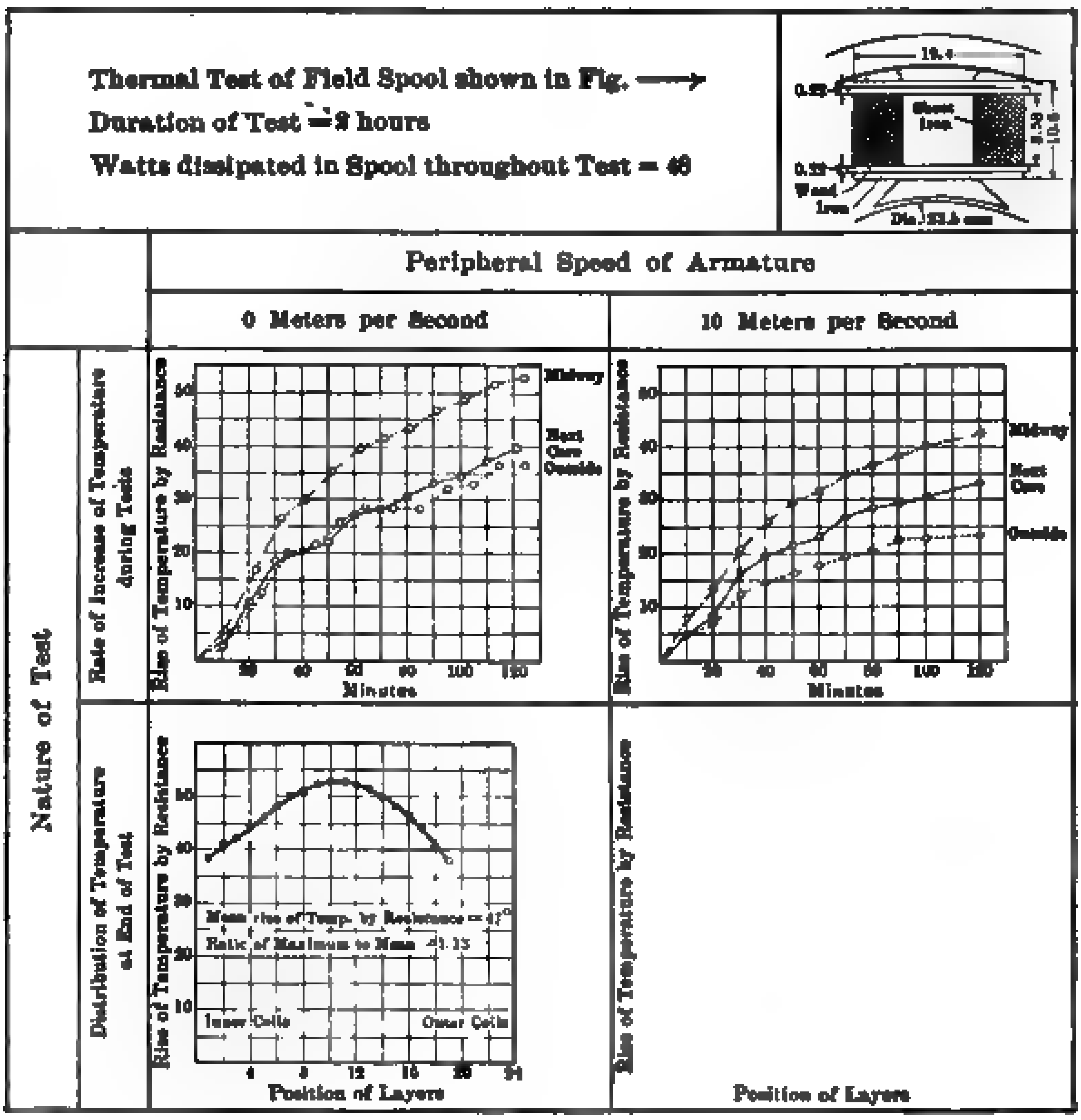


FIG. 24.— Temperature tests of field spools. (Reproduced by permission from the *Journ. I. E. E.*, vol. 38, p. 421).

out by the test results for a field coil of a certain four pole continuous current machine. These results are plotted in Fig. 24. The coil was wound with 38 layers of No. 21 B.W.G.; connecting leads were brought out every two layers, so that the resistance of 19 sections of the coil could be independently determined by means of their rise in resistance. Two separate tests were made, one with the

armature stationary, and the other with the armature revolving at a peripheral speed of 10 metres per second. We see from Fig. 24 that when the armature was at rest, the layer next to the magnet core and the outside layer had practically the same temperature, but that when the armature was running at a peripheral speed of 10 metres per second, the outer layer had a much lower temperature than the layer next the magnet core.

In both tests, however, it should be noted that the temperature at the middle of the winding is considerably higher than the temperature of either the internal or the external surfaces.

When the armature was stationary the ratio of the maximum temperature rise to the mean temperature rise was 1.13, and when the armature was rotating, this ratio was about the same figure, being actually 1.15.

But another relation that is very striking, is that of the maximum temperature to the temperature at the external surface. This works out at 1.4 and 1.95 respectively, for the tests when the armature was stationary and revolving. The fact that the internal temperature of a coil may rise to a value twice as great as that which would be indicated by a thermometer laid on the external surface, ought to impress engineers with the importance of determining all rises of temperature of coils by means of the alteration in resistance. This is the more important in view of the importance of avoiding liability to deterioration of the insulation on the field conductors.

Some tests made by Dettmar in 1900 on the effect of temperature on the cotton coverings of copper wires showed that, in the course of time, a temperature of less than 100 deg. Cent. caused decided deterioration of the cotton coverings. The more recent tests of the National Physical Laboratory of Great Britain * on large numbers of insulating materials, showed that in the case of all these materials, deterioration ultimately set in at a temperature of not over 125 deg. Cent., and in most of these materials the temperature at which deterioration occurred was considerably below 125 deg. Cent.

These curves and values for the distribution of temperature in an ordinary field coil emphasize the importance of ventilating the field spools by subdividing them into two or more parts with concentric air channels between them. Field coils for modern machinery should be constructed in this manner, which ensures a more uni-

* See *Journ. I. E. E.*, vol. 34, p. 613.

form distribution of temperature throughout the depth of the coil, and also a considerably lower average temperature for a given expenditure of copper or a smaller weight of copper for a given mean temperature rise.

In the case of turbo-alternators, the field windings usually form part of the rotating system and the methods of winding and fixing the coils depart considerably from the ordinary field coil construction. Among the various constructions employed for turbo-alternator fields (which are described in a subsequent chapter), the types vary from the ordinary, compact field coil, through variations and developments from this, to a thoroughly distributed field winding in slots similar to those on an armature, which is employed with the smooth cylindrical type of rotating field.

It is necessary to consider each case or type individually, with due regard to the velocity of the moving windings, the distribution, and conductivity of the coil and of the material in contact therewith, and also the ventilating provisions.

For an ordinary field coil rotating at a mean peripheral speed of about 40 metres per second, the specific temperature rise at the centre of the body of the coil, may be from 15 to 30 deg. Cent.

In turbo-alternators, the number of poles is generally few, being determined by the frequency and speed, from 2 to 8 being practically the limits. With such fields carried out with the definite pole construction, there is plenty of space between the poles and free access to the cooling air. When the field winding is carried out in slots on a cylindrical core it may be treated as equivalent to a wound armature from the heating standpoint although it is not generally so well ventilated. In such machines it is instructive to calculate the heating coefficient mentioned on p. 36 of this chapter, viz. the total stator and rotor losses per square decimetre of air gap surface.

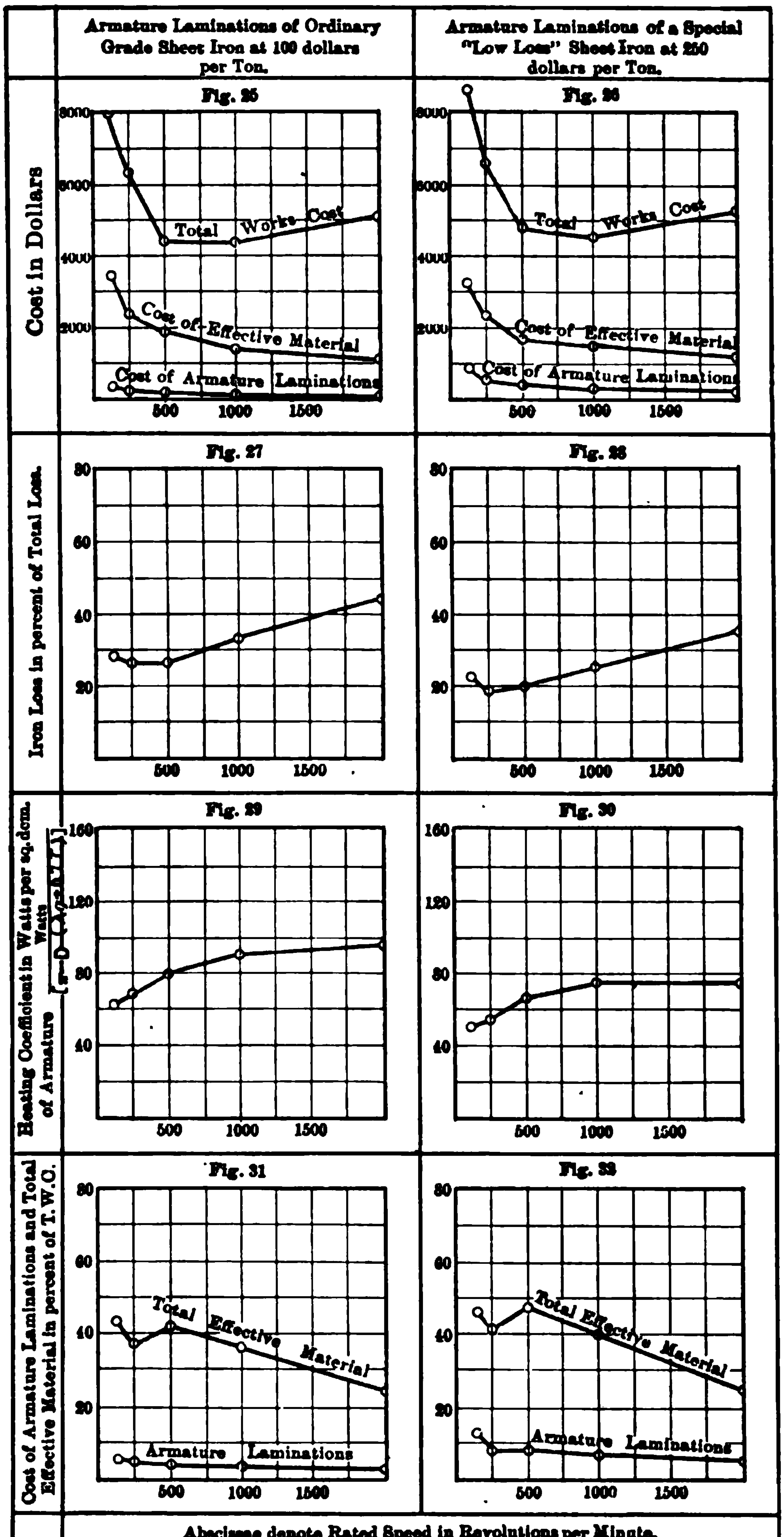
CHAPTER IV.

MATERIALS FOR CONSTRUCTION OF HIGH SPEED ELECTRIC MACHINES.

Armature Core Plates. *Core Loss.* — A number of groups of designs of alternating and continuous current dynamos are analysed in later chapters of this treatise. These designs show conclusively that one of the most striking characteristics of high speed dynamo electric machines of large rated outputs is the very large percentage of the total internal loss constituted by the core loss. In the case of turbo-alternators the core loss frequently amounts to two thirds and more of the total loss, and in many cases it cannot be materially reduced. In slow speed alternators of the same rated capacity, on the contrary, the core loss is more of the order of 50 per cent or less of the total internal loss.

During the last two years there have been placed on the market some grades of sheet iron which, in practice, permit of reducing the core loss for a given design to some 60 per cent to 70 per cent of the amount when the more customary grades of sheet iron are employed. Unfortunately the price of the low loss material is at present from two to three times that of the material customarily employed for armature cores. In view, however, of the reduction of loss, and consequently also of heating, and of the improved efficiency at all loads, the low loss sheets are to be recommended for the armatures of high speed alternating current machinery in spite of the high price.

The use of this iron may often also assist the designer of high speed *continuous current* machinery, for the question of heating has to be handled with much greater care in the design of high speed than in the design of low speed continuous current machines. As to the cost of the sheets, this, while high, will not so greatly affect the total cost of the design of these machines as might at first be thought would be the case, for it is a characteristic of high speed continuous current dynamos that the cost of the electromagnetic material is generally a somewhat less percentage of the Total Works



Figs. 25–32.—Curves showing effect of grade of armature laminations, on cost and quality of continuous current generators for 1000 kw., 1000 volts, and various rated speeds.

Cost than is the case with the low speed machinery. Thus quite a considerable increase in the outlay for core plates will in many cases only entail a fairly small percentage increase in the Total Works Cost. This is clear from the curves in Figs. 25 and 26 relating to designs for 1000 kw., 1000 volt continuous current dynamos for various speeds, and in which respectively the customary and the low-loss grades of core plates are employed.

We see from the curves that the Total Works Cost of the designs is only increased by some 5 per cent to 10 per cent by the substitution of the superior grade core plates. The consequent improvement in the quality of the design is generally somewhat greater the higher the rated speed. This is seen from the curves in Figs. 27 and 28, in which the core loss is plotted as a percentage of the total internal loss. Thus the higher the rated speed the greater is the desirability of employing the low loss iron, although in the case of continuous current machines the gain is by no means great. Figs. 29 and 30 show the relative heating coefficients for the armatures in the two cases and Figs. 31 and 32 the costs of the armature laminations and of the total effective material as a percentage of the Total Works Cost. Fig. 33 shows the ratio between the Total Works Cost in the two cases.

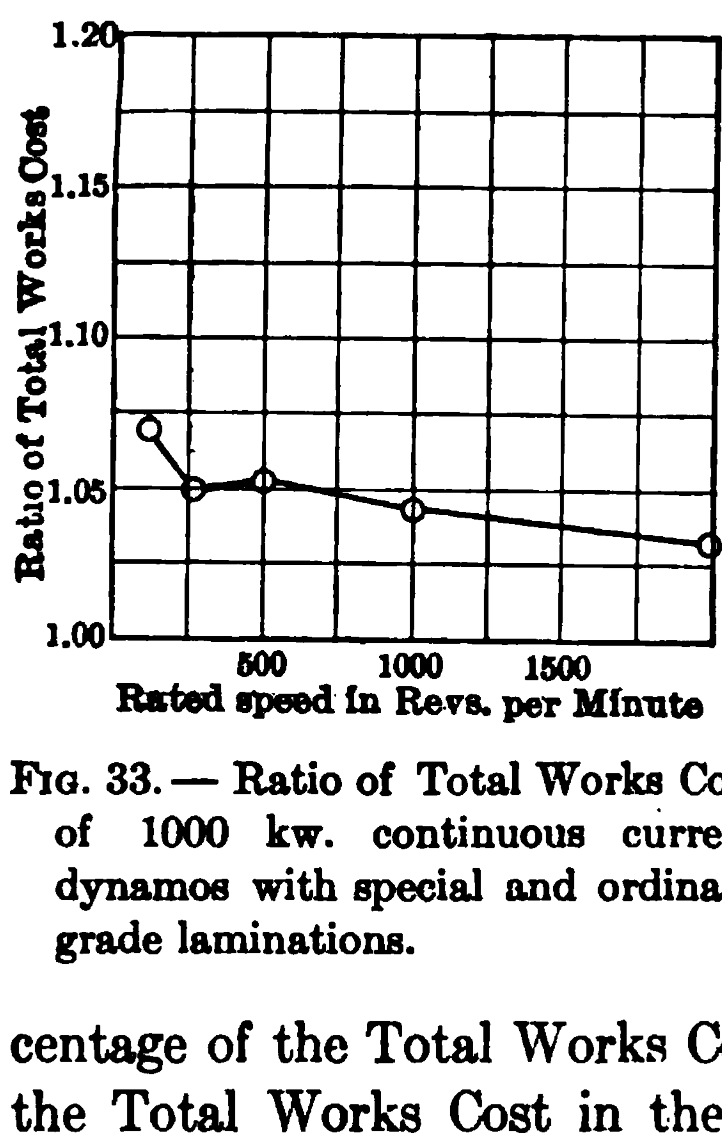
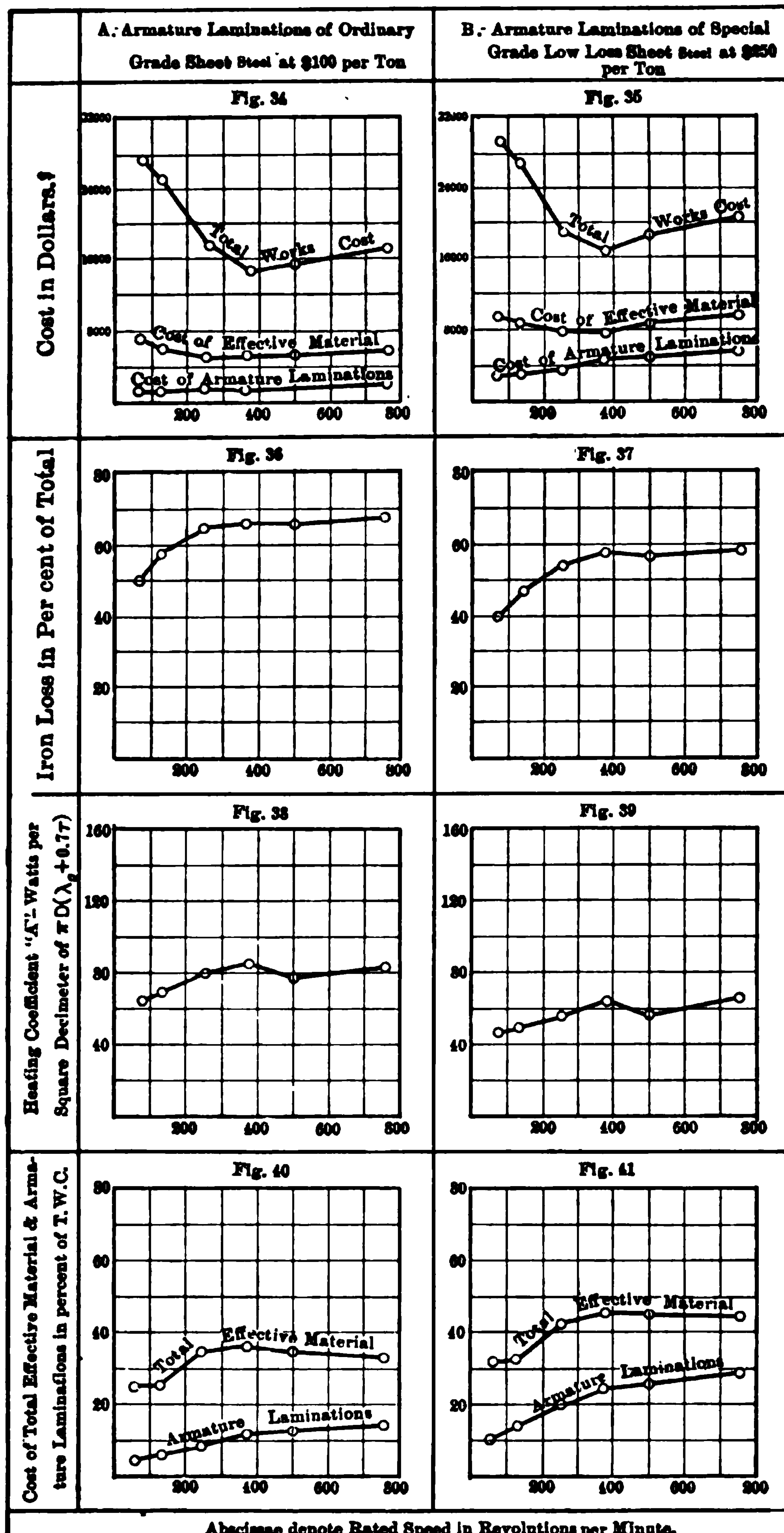


FIG. 33.— Ratio of Total Works Cost of 1000 kw. continuous current dynamos with special and ordinary grade laminations.

In high speed alternators, the core loss assumes much greater proportions than in continuous current machines. This is largely due to the armature being external to the rotor, and of the great radial depth of the laminations as a consequence of which the weight of laminations for given air gap dimensions, D and λg, is much greater than in the case of an internal armature.

Figs. 34–42 show in detail the manner in which the cost, losses, and heating for a group of 3000 kva. 25 cycle alternators at different rated speeds are affected by the use of the special low loss steel laminations. Further details of these machines are given in Chapter IX.



FIGS. 34–41.—Curves showing effect of grade of armature laminations on cost and quality of 3000 kva. alternators for various rated speeds.

In these figures the various quantities are plotted against the rated speed of the machine.

Fig. 34 shows the Total Works Cost of the machine, the cost of the effective material and the cost of the armature laminations for ordinary grade stampings. In Fig. 35 are shown similar curves for the same machines when built with the low loss sheet steel stampings. Figs. 36 and 37 give the iron loss as a percentage of the total electrical losses. Figs. 38 and 39 show the armature heating coefficient. In Figs. 40 and 41 are plotted the costs of the effective material, and of the armature laminations as a percentage of the Total Works Cost of the machine. Fig. 42 shows the ratio of the estimated Total Works Costs of a machine with low loss iron to the Total Works Cost of a machine built with stampings of ordinary grade iron.

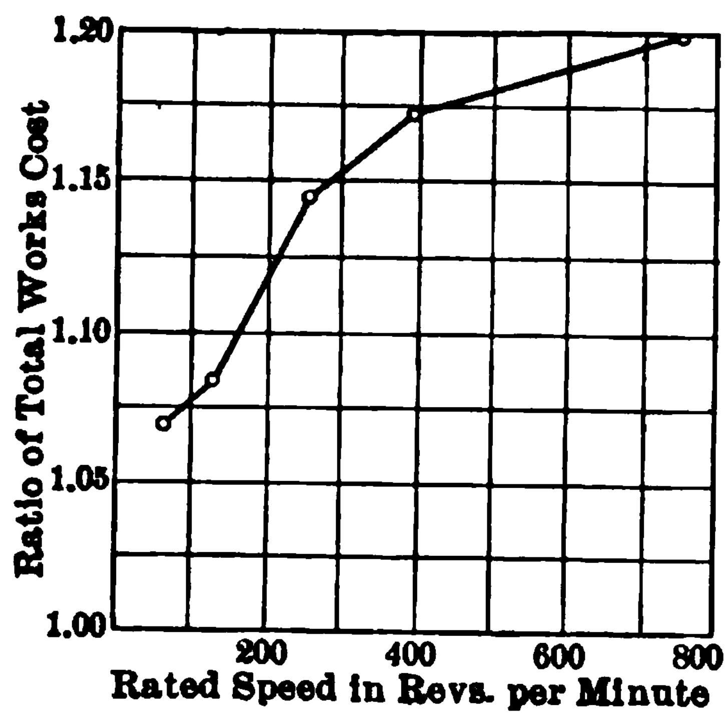


FIG. 42.— Ratio of Total Works Cost of 3000 kva. alternating current generator with special and ordinary grade armature laminations.

All the above curves are based on the assumption that the same weight of material is used in both cases; the improvements effected by employment of special low loss steel, lying in the direction of the quality of the design and not of the cost.

In general it may be seen from the curves that in the case of alternators, the higher the speed the greater is the percentage core loss, and the greater also is the percentage cost of the armature laminations. The result is that though it is particularly desirable to use the low loss steel at the high speeds yet, at the same time, the extra cost involved by the use of this expensive material becomes considerably greater.

Thus in Fig. 42 the ratio of the Total Works Cost for the two cases rises to as high a value as 1.2, at the highest speed. This is due to the large proportion of the total weight of the machine which the armature laminations constitute in the case of alternators for high speeds and with few poles.

A similar set of curves are shown in Figs. 43–51 deduced for the 400 kva. 50 cycle alternators described in Chapter VIII.

The same quantities are plotted in these figures. The range of speed is much greater, and the ratio of Total Works Costs rises to the value of nearly 1.3, that is, the cost of the machine is increased by nearly 30 per cent by the employment of the special grade iron for the armature laminations. This high figure is here due to the fact that it relates to a 2-pole design in which the armature iron constitutes a very great percentage of the total weight.

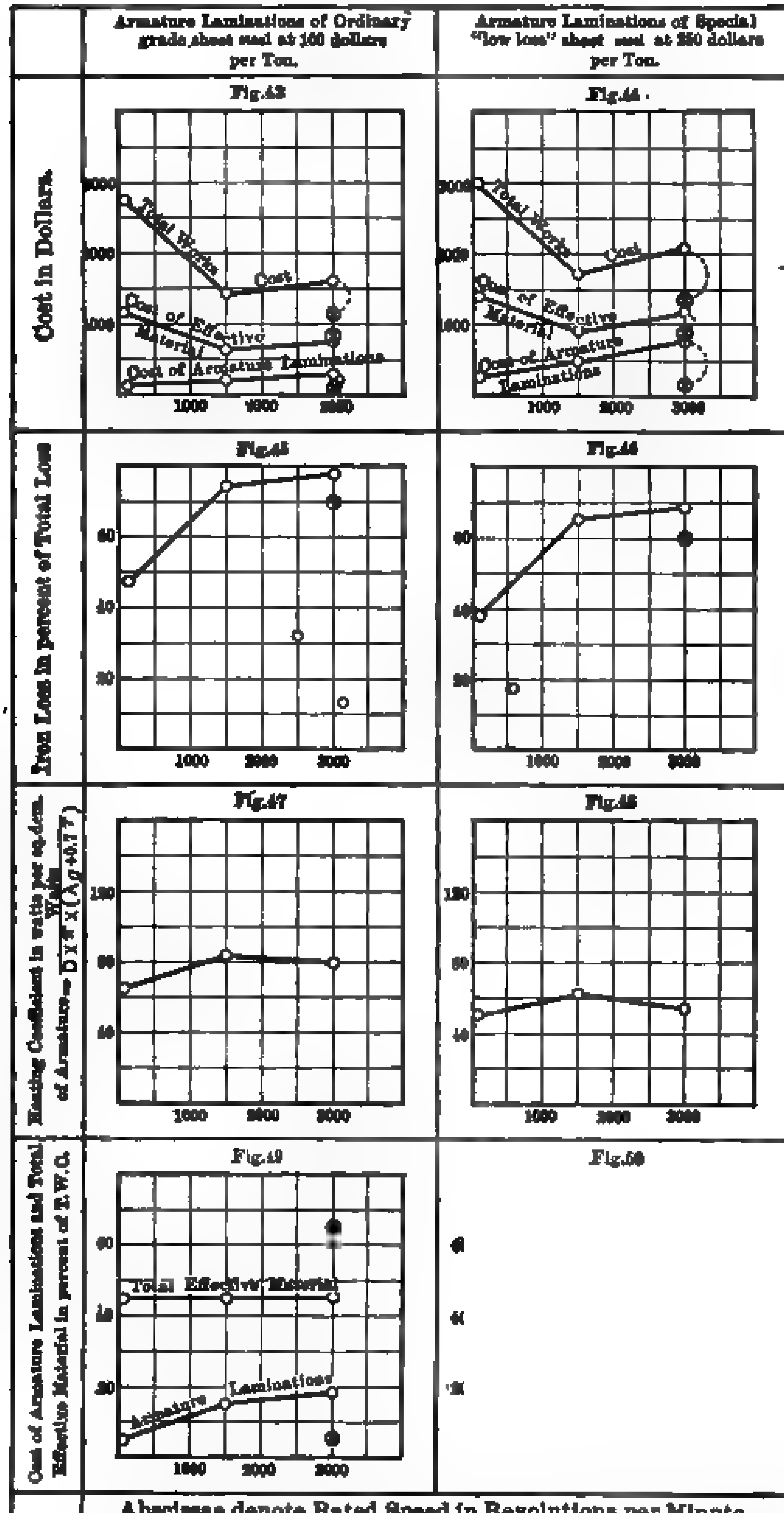
It is, nevertheless, especially in 2-pole designs that the use of a low loss iron is most important, as such machines are at best of but poor quality and in such cases the improvement in quality is justified even at the expense of considerable increase in the Total Works Cost.

We see then from the above that the employment of special grade steel for the armature laminations of alternators is, so far as relates to expense, less extravagant, the slower the speed, but that it is more desirable, or even indispensable, so far as relates to the quality of the results, the higher the speed.

The reason for the considerable increase in the Total Works Cost which the use of special grade steel effects in the case of alternators is simply due to its high price. When the use of such material has become more common, the price will be brought more into line with that of the present ordinary grades of steel, and its use for high speed alternators will become general.

In the designs set forth in this treatise we have based our estimates on the use of the customary grades of core plates. In so far as the low loss core plates are employed instead, the designs will be improved. But the designs will even then be none too good, for in the design of extra high speed electrical machinery, there is no option but to sail closer to the wind than in designing machines for less extreme speeds, so that the increased margin afforded by the use of this low core loss material is very welcome.

The "figure of loss" is the most useful term in which to express the quality of sheets as regards core loss. This term is defined by the Verband Deutscher Elektrotechniker as the total iron loss in watts per kilogram, as measured by means of a wattmeter on a sample made up of at least four different plates. The sample shall weigh at least 10 kg. and the loss in watts per kilogram shall be determined at a temperature of about 30 deg. Cent. for a maximum induction of 10 kilolines per square centimetre and a periodicity of



FIGS. 43–50. — Curves showing effect of grade of armature laminations on cost and quality of 400 kva. alternators for various rated speeds. (The points ⊗ relate to the rotating armature designs of column D of specification on pp. 133–137.)

50 complete cycles per second: As normal thicknesses shall be taken 0.3 mm. and 0.5 mm., deviations from the normal thickness shall not exceed 10 per cent. The measurements shall be made on a magnetic circuit composed exclusively of iron or steel, of the quality to be tested, and built in accordance with the conditions set forth below. As specific weight of the iron, 7.77 shall be taken in all cases where more precise data is not available. The iron loss shall be measured by the Epstein method in accordance with which the magnetic circuit is constructed of four cores, each having a length of 500 mm., a breadth of 30 mm., and a weight of at least 2.5 kg., thus making a total weight of at least 10 kgs. for the four cores. The individual sheets are insulated from one another by Japanese paper in such a manner that they are at no point in contact with one another. The four cores constitute a rectangular circuit, as shown in Fig. 52, and are secured in position by wooden clamps at the four corners. At the butt joints they are separated from one another by press-spahn of 0.15 mm. thickness. In building together the circuit, care must be taken that the cores fit well with one another. As indications that this is the case may be mentioned the deadening of the noise when magnetised, and the obtaining of a minimum deflection on the ammeter in the magnetising circuit.

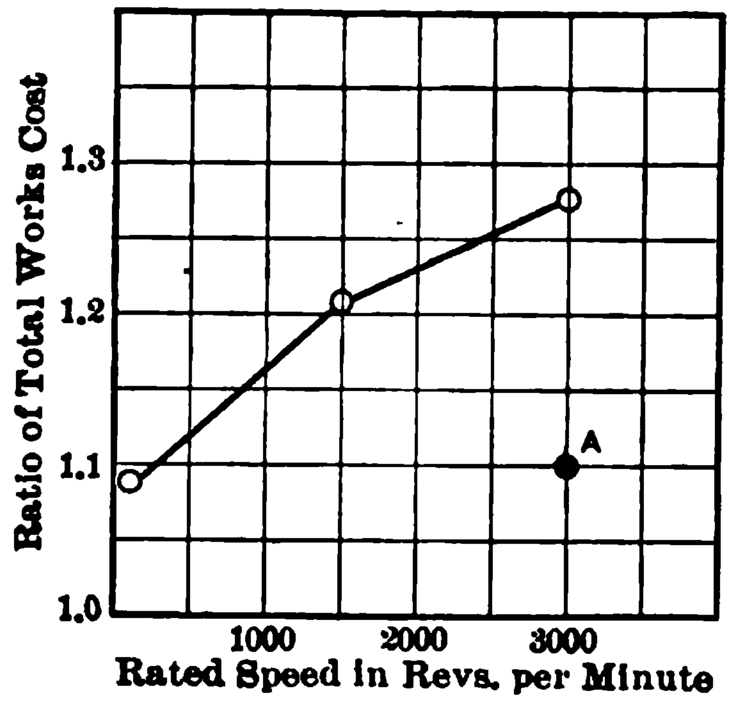


FIG. 51.— Ratio of Total Works Cost of 400 kva. alternating current generator with special and ordinary grade armature laminations. (Point A relates to a rotating armature design.)

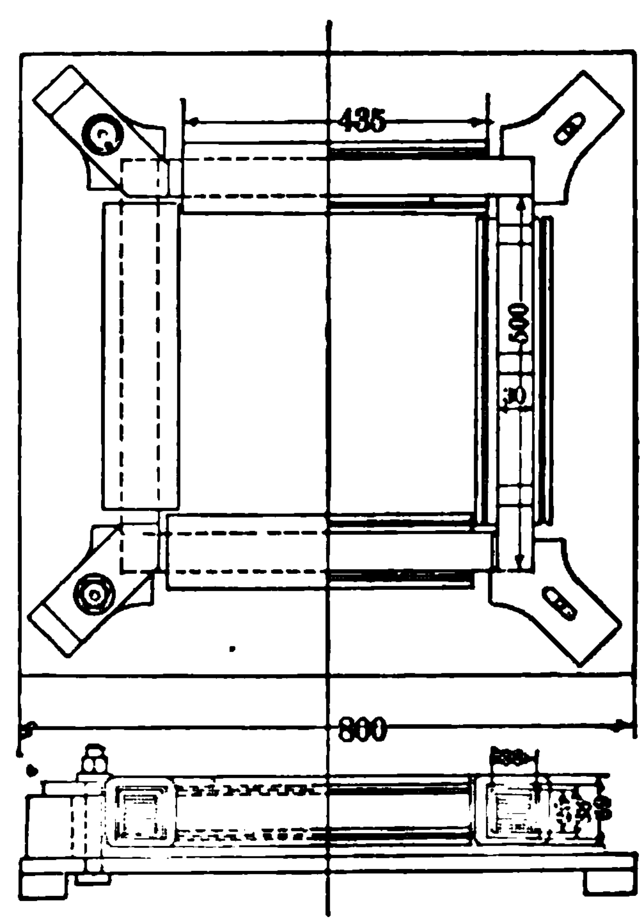


FIG. 52.— Epstein sheet iron tester.

The magnetising coils are constructed on bobbins, with internal dimensions of 38 mm. × 38 mm. and a length of 435 mm. Each of

the four bobbins contains 150 turns of copper of a cross section of 14 sq. mm. It is suggested in the rules that the winding may conveniently consist of two round wires each of 3.5 mm. diameter and wound in parallel. Originally it was thought preferable to build up these Epstein samples to a weight of 20 kgs., but this weight has ultimately been reduced to 10 kgs., which is now standard. A photograph of a wound sample is shown in Fig. 53.

FIG. 53.— Wound sample of Epstein iron tester.

The most customary standard size in which sheet steel for armatures is delivered, is in plates measuring 1 metre $\times$ 2 metres. Plates of larger dimensions are generally only supplied at higher prices, and a longer time is required for providing them. It has thus become fairly general practice when armature cores of diameters in excess of 1000 mm. are required, to build them up from segments whose maximum dimensions do not exceed the dimensions of sheets of this standard size.

It is therefore customary to consider 990 mm. as the largest diameter of armature in which complete disks shall be employed.

The "figure of loss" for ordinary sheet iron ranges from 1.5 to 4 watts per kg. From the standard conditions of density (10 kilolines per square centimetre) and frequency (50 cycles per second) at which (F) the "figure of loss" is expressed, the loss in watts per kilogram at any other density (B) and frequency (N) may be approximately estimated as follows:

Let L equal the loss for any other than the normal values of B and N (i.e., at values other than $B = 10$ and $N = 50$), for an iron whose figure of loss is equal to F. Then $L = \frac{C}{10 \times 50} \times N \times F = 0.002 \times C \times N \times F$, where C is a factor depending on the density B. Letting $K = 0.002 \times C$, we have $L = K \times N \times F$. Values for K for any density, B, may be taken from Table 8.

TABLE 8.

VALUES OF K IN THE FORMULA $L = K \times N \times F$, FOR OBTAINING THE CORE LOSS IN WATTS PER KILOGRAM.

Density in Kilolines per Square Centimetre. B	Multiplier to Obtain the Watts per kg. from Figure of Loss and Periodicity. K	Density in Kilolines per Square Centimetre. B	Multiplier to Obtain the Watts per kg. from Figure of Loss and Periodicity. K
4	0.0046	13	0.0296
5	0.0066	14	0.0336
6	0.0088	15	0.0380
7	0.0114	16	0.043
8	0.0136	18	0.050
9	0.0168	20	0.060
10	0.0200	22	0.070
11	0.0236	24	0.080
12	0.0268		

The watts per kilogram for customary material for a thickness of some 0.4 to 0.5 millimetre and at a periodicity of 50 cycles per second, is plotted as a function of the density in curve A of Fig. 54. Curve B of this same figure corresponds to one of the low loss materials. The figures of loss for these two materials are indicated in the curves, and in these tests were respectively 3.3 and 1.6.

When the laminations are built up together, as in a finished machine, the actual core loss per kilogram is considerably greater than would correspond to the results obtained on samples as plotted in Figs. 54 and 55, from tests by the Epstein method or its equivalent.

Measurements of the core loss of actual machines do not show so great a gain from the use of low loss iron as are indicated by measurements on samples. It is found that the low loss iron may be relied upon to reduce the core loss in the actual machines to some 60 per cent to 70 per cent of the loss with the customary grades of core plates.

This lower degree of superiority may be ascribed largely to the eddy current losses due to filing the slots of the assembled armature and to eddy current losses in various solid parts as also in the armature

conductors. These additional (or "parasitic") losses will not be decreased through the use of the better quality of core plates, and hence they tend to mask the advantage to be gained by employing it. The residual gain is, however, ample justification for employing the low loss material in extra high speed alternators, notwithstanding its great cost, as the difficulties associated with such designs justify resorting to any sound expedient not leading to prohibitive Total Works Cost.

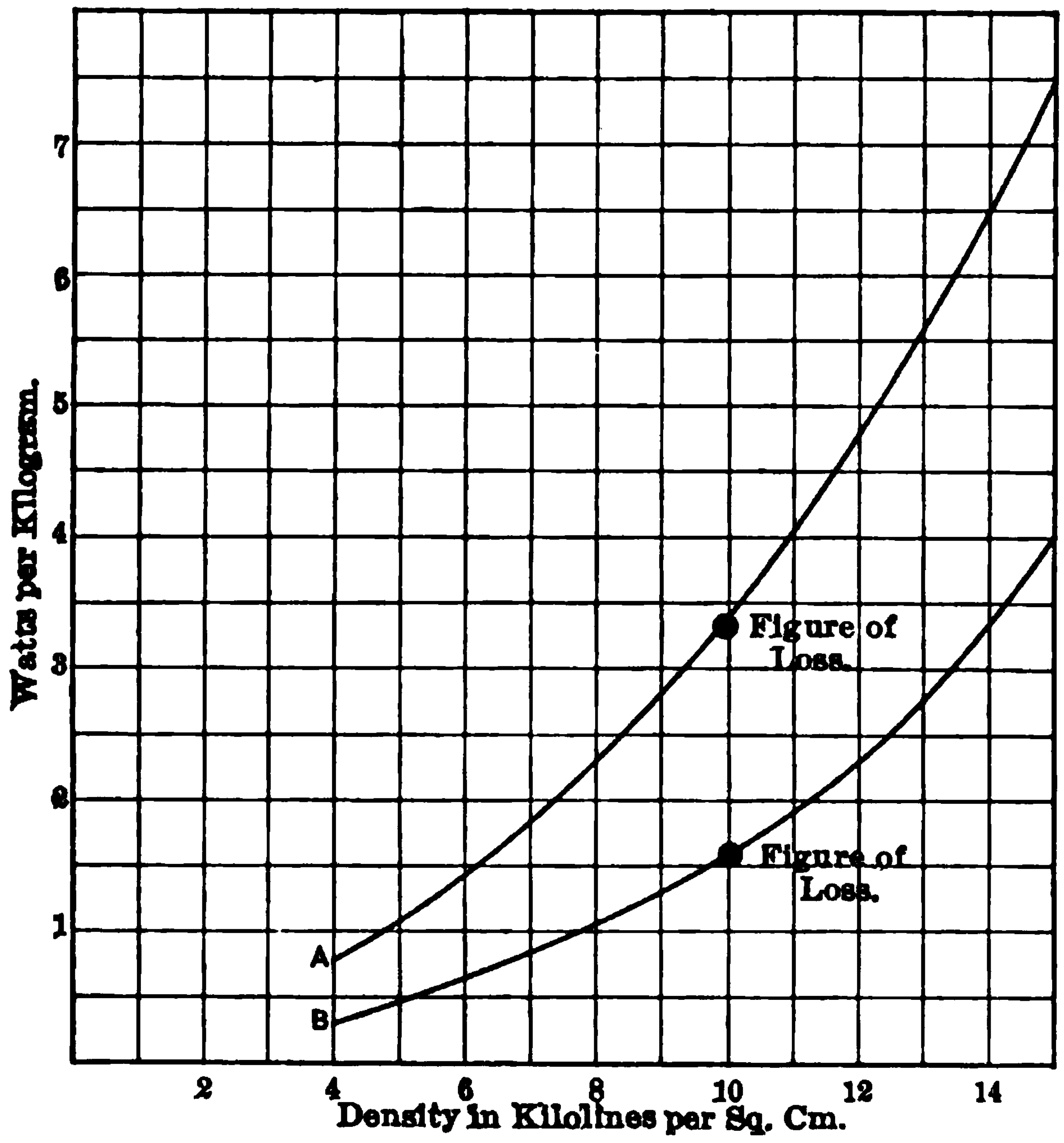


FIG. 54.— Curves showing the energy losses in armature stampings (thickness 0.4 to 0.5 mm.). For periodicity of 50 cycles and for different densities.

The core loss varies with the thickness of the plates, and the curves in Fig. 55 (taken from test results supplied by a large dealer in core plates) give approximate data of the extent of the influence of the thickness. The term "Stalloy" appended to the lower curve is merely a trade name applied to a particular brand of low loss core plates. Other dealers in armature core plates supply brands with substantially identical properties, and there are beginning to be heard reports of even better results.

The eddy current loss depends on the specific resistance of the

iron, a high resistance iron showing a lower eddy current loss. This property has been utilized to produce the "low loss" sheet steels which have appeared during the last year or two. The properties of such steel alloys embody a high specific resistance — up to 45 microhms per cubic centimetre and more, with very slight inferiority in magnetic permeability. The core loss for such an alloy is plotted in the lower curve of Fig. 54, from which it will be seen that the loss as measured on samples for this alloy, is some 50 per cent of that obtained with ordinary iron.

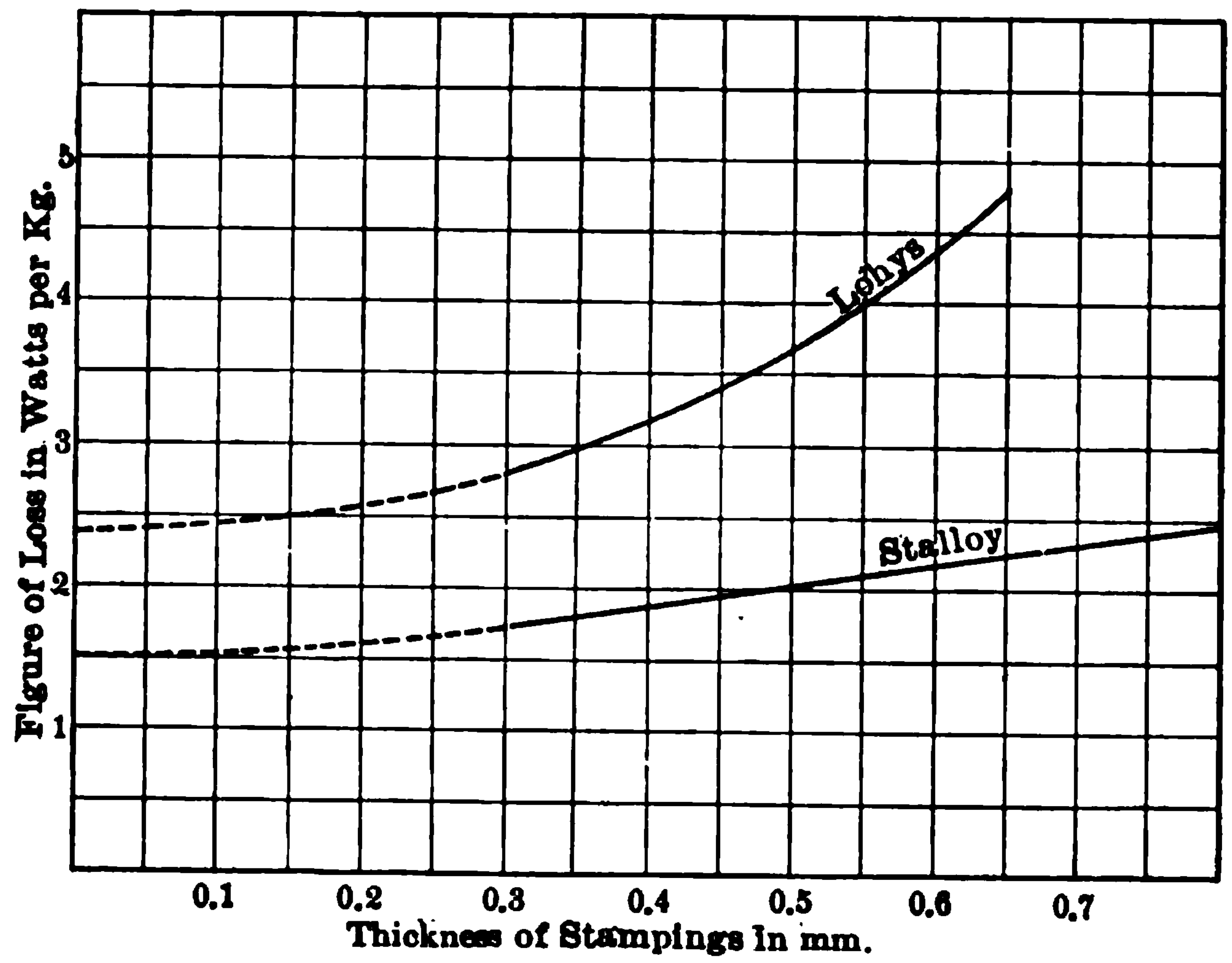


FIG. 55. — Curves showing the effect of the thickness of the stampings on the figure of loss (watts per kg. at a periodicity of 50, and a density of 10,000) for stampings by Messrs. Sankey of Belston, England.

If we produce the curves in Fig. 55 until they cut the vertical axis, as indicated by the dotted lines, we obtain a theoretical value for the figure of loss when the laminations are infinitely thin; in such a case, however, the electrical resistance to the eddy currents would be infinitely great, and the energy wasted in such currents would be infinitesimal. Hence we can obtain some idea of the amount of the hysteresis component of the losses; the figure of loss for the "Lohys" grade is about 2.45 watts per kg. at the limit, and on the other hand the "Stalloy" brand has a figure of loss of only about 1.5 watts per kg. Thus not only do these low loss steels have greatly reduced eddy currents due to their high specific resistance, but the hysteresis loss is also materially decreased.

Such a low loss steel may be utilized in high speed machines either by retaining the same weight of iron, and thus reducing the core loss and the heating, or by retaining the same core loss and heating and thus reducing the weight. If the design is sufficiently good from the thermal standpoint, the latter plan may be advisable, but it would depend on the relative cost in each case. The price of ordinary grade sheets ready for stamping is at present from $70 to $100 per ton, and for the low loss iron from $180 to $220 per ton.

In the case of a 4-pole 650 kva. alternator designed in Chapter VII, the magnetic cross section in the armature is 1540 square centimetres and the weight of armature stampings is 2.34 tons. At a density of 8,000 lines per square centimetre and a frequency of 50 cycles, the loss per ton is 7 kw. and the total core loss 16.4 kw. As the heating coefficient of this machine is not excessive (80 watts per square decimetre of air gap surface) it is not necessary to decrease the core loss, and the armature laminations might be replaced by low loss iron at a density of 12 kilolines, which would require a magnetic cross section of only 1030 square centimetres. The weight would then be 1.55 tons and the loss per ton 10.5 kw., giving a total loss of 16.3 kw., which is the same as before. With the low loss iron the external diameter of the armature stampings would be reduced from 128 to 115 centimetres, and consequently a smaller frame could be employed with somewhat less material. For these two cases, however, the cost of the stampings will be some $210 and $350 respectively, so that the outlay for core plates is some $140 greater.

In the case of bi-polar alternators, we have seen that the heating coefficient is rather high, and very efficient ventilation is called for. The bulk of the armature losses is comprised in the iron loss, which reaches some 90 per cent of the total armature loss. Advantage may in these cases be taken of a low loss iron by substituting stampings of the same magnetic density and the same total weight, which would give a diminished core loss and heating. As a case in point, let us consider the 400 kva. 2-pole alternator, for which data is given in Chapter VIII. The heating coefficient for this machine is 152 watts per square decimetre. A comparison between the design as it stands and as it would be with special iron is set forth in Table 9 on page 58.

Permeability of Magnetic Materials. — For the designer, the most convenient method of showing the relative permeability of the various magnetic materials, is by means of saturation curves showing

the excitation required for unit length of the material at different induction densities. Saturation curves for cast iron, cast steel, and armature laminations are shown in Fig. 56. While it is quite possible to obtain materials having a somewhat higher permeability than is indicated by these curves, there is always an element of uncertainty, owing to variations in the composition of the material. It is therefore not expedient to base designs on the higher values obtainable,

TABLE 9.

COMPARISON OF COSTS OF ARMATURE, OF 400 KVA. 2-POLE ALTERNATORS.

	Ordinary Iron.	Special Iron.
Magnetic cross section — sq. cms.	2050	2050
Flux density — kilolines per sq. cm.	8.2	8.2
Frequency	50	50
Kilowatts per ton	7.4	4.9
Weight — tons	2.32	2.32
Total core loss — kilowatts	17.2	11.5
Total armature losses (iron and copper)	19.96	14.26
Heating coefficient — watts per sq. dm. of $\pi D\lambda g$	152	108
Full load efficiency ($\cos \phi = 1$)	94.5%	95.5%
Cost of stampings	$210	$500

unless the material is carefully tested before use and the poorer samples ruthlessly rejected. The curves in Fig. 56, however, have been carefully prepared with a view to ensuring conservative results.

Materials for Rotors. — Rotating armatures for continuous current machines are of laminated construction and there is no particular difficulty in obtaining homogeneity.

The stresses in a rotating armature for a continuous current machine are not generally excessive, as continuous current machines do not usually run into large sizes. Further, the windings are thoroughly distributed, which relieves the system of the stresses due to large concentrated masses of copper. With rotating fields the stresses are generally of greater magnitude, being in some cases of such values that the unavoidable working stress does not permit of a safety factor of much more than 2, referred to the elastic limit.

In view of this, the material must, regardless of cost, be of the best quality procurable as regards strength and homogeneity. Hence for all high speed machines, except perhaps for those of small size, castings should generally be avoided. The material for rotating fields

may be either steel forgings, pressed steel, or sheet steel stampings, according to the type of construction employed.

The safety factor obtained depends on the size and speed of the machine. Were it practicable to employ extra high strength steels, such as are used in rotating disks for steam turbines, considerably higher safety factors could be allowed. Such high strength steels are obtained by the addition of suitable small percentages of foreign materials — such as carbon, nickel, manganese, and chromium.

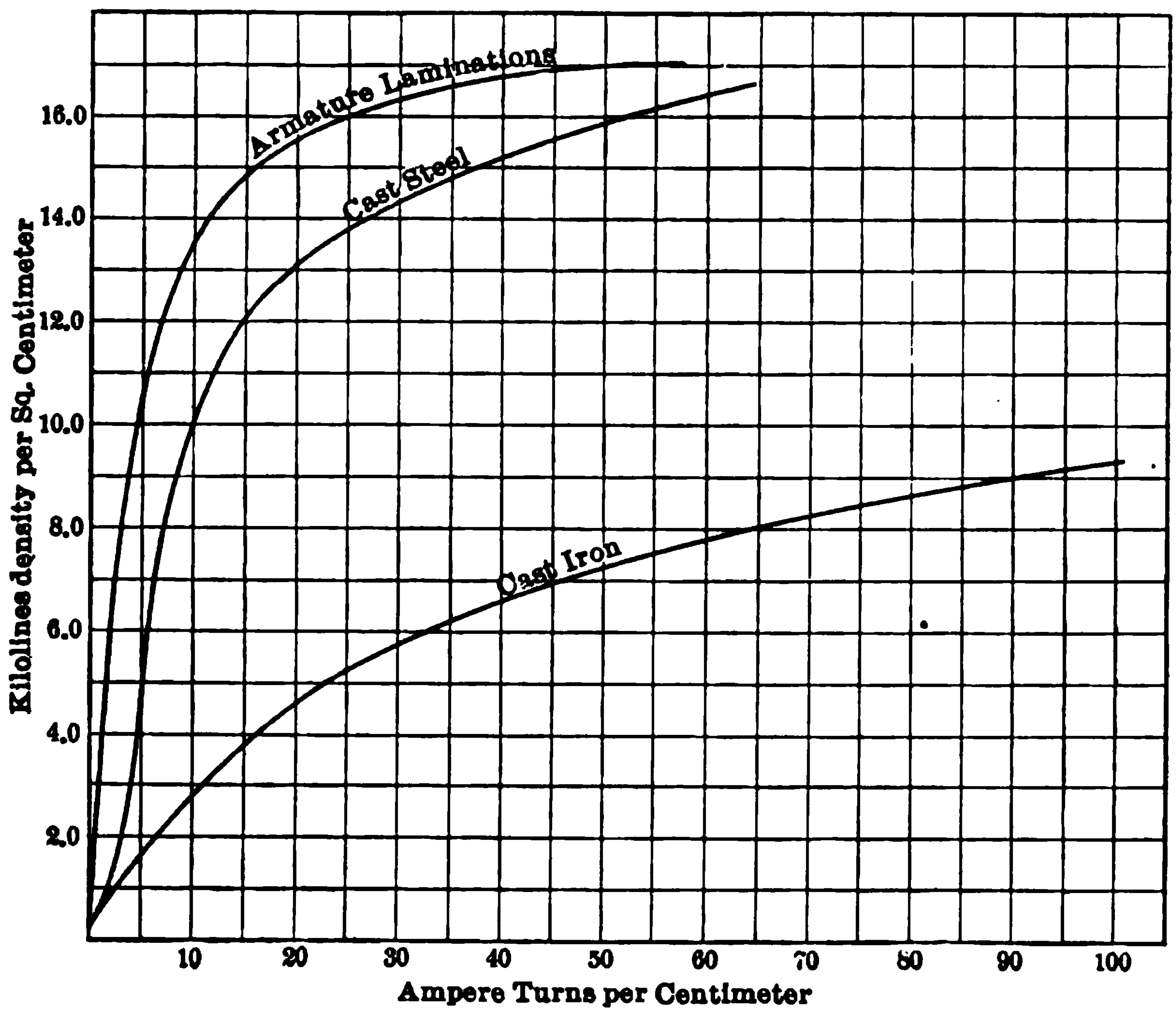


FIG. 56. — Saturation curve used in designing the magnetic circuit of electric machines.

These substances, when used in the proportions required for imparting suitable mechanical properties, unfortunately impair the permeability and magnetic properties of the steel.

It will be of interest to give a few notes on materials of great mechanical strength. Stodola in "Die Dampfturbinen" quotes figures supplied by the Krupp Works at Essen. For turbine disks, a nickel steel is recommended of some 9 tons per square centimetre breaking strength with 12 per cent elongation, and an elastic limit of 6.5 tons per square centimetre. Nickel steel of greater strength

(though with less percentage elongation), is available, and with forged pieces of small dimensions a breaking strength of over 20 tons per square centimetre, and an elastic limit of over 6 tons per square centimetre can be obtained. An average of 6 tests showed a breaking strength of 18.1 tons per square centimetre, and an elastic limit of 13.2 tons per square centimetre with a mean elongation of 6 per cent. Messrs. Krupp's opinion is that where there is no reversal of the direction of the stress, it is permissible to work up to one third of the elastic limit, and ultimately perhaps even higher. Doctor Riedler and Professor Stumpf consider that from 2 to 2½ as a safety factor is admissible.

With forgings of ordinary steel, such as is permissible for rotating fields, the average breaking stress in tension is about 4.7 tons per square centimetre, and the elastic limit is 3.5 tons per square centimetre as set forth in Table 11 on pages 62 and 63. Hence allowing a safety factor of 2.5 on the elastic limit, a value of 1.4 tons per square centimetre for working stress in tension is obtained.

The peripheral stress in a rotating cylinder is given by the expression

$$f = \frac{dv^2}{98{,}100}.$$

Where f is the peripheral stress in tons per square centimetre, due to centrifugal force, d is the density of the material in grams per cubic centimetre, and v is the peripheral speed in metres per second.

TABLE 10.

THE LIMITING PERIPHERAL SPEED FOR MATERIALS OF DIFFERENT STRENGTH.

Material.	Peripheral Speed in Metres per Second for Peripheral Stresses Equal to	
	Ultimate Strength.	Elastic Limit.
Wrought iron	220	160
Cast iron	130	90
Cast steel	220	150
Forged steel	240	170
Cast copper	130	...
Rolled copper	155	80
Hard drawn copper	210	...
Cast brass	140	...
Gun metal	159	75
Phosphor bronze	210	125
Manganese bronze	230	...
Delta metal (cast)	240	120
Cast aluminium	220	...
Wood — Pine	260 to 370	...

This approximates to $f = 0.00008v^2$ for materials of the same specific gravity as steel and iron.

Hence for a stress of 1 ton per square centimetre, the corresponding peripheral speed will be about 110 metres per second. A peripheral speed of 100 metres per second is rarely exceeded in alternator designs, and considerably lower speeds are highly desirable.

From this relation we are able to calculate the speed corresponding to the breaking stress and the elastic limit. These limiting speeds for various materials are set forth in Table 10, the ultimate strength and elastic limit being taken from Table 11.

The windings, whether on rotating armatures or fields, are generally so disposed as to minimise the stress on the copper as far as practicable. The centrifugal forces of windings distributed in slots are taken up by slot wedges of bronze or gun metal, or, if the stresses are low, oak or maple wood. The end covers for armature windings and also for field windings of fields of the smooth cylindrical type, may be of phosphor bronze, manganese bronze, or nickel steel.

Table 11 gives for a number of materials, including all those referred to above, data for the average working and breaking stresses and for the elastic limits.

There has been a general impression that the low loss sheet steel, of which considerable data has been given in this chapter, is rather inferior as regards mechanical strength, and that this inferiority might stand in the way of its use in rotors where the highest obtainable mechanical strength is of prime importance. While this work has been going through the press we have had pointed out to us that, at any rate in the case of certain of these new low loss sheet steels, this is not the case, and there have been supplied to us the following comparative figures for physical tests of "Stalloy" and of ordinary soft steel.

	Stalloy.	Ordinary Armature Quality.
Maximum load in tons per sq. in.	32	18
Elastic limit in tons per sq. in.	26	10
Elongation on 4 in.	10%	18%

Messrs. Joseph Sankey & Sons, the manufacturers of stalloy, also point out that, realizing that low hysteresis is not of much conse-

quence in the rotor, they have been making a special quality of steel purely with respect of mechanical strength, for which the figures are approximately as follows:

Maximum load in tons per sq. in.	25
Elastic limit in tons per sq. in.	17
Elongation on 4 in.	10%

The manufacturers do not guarantee the magnetic quality of this latter material. With stalloy, however, it would appear from the figures that both high mechanical strength and good magnetic quality are secured.

TABLE 11.

AVERAGE BREAKING AND WORKING STRESSES AND ELASTIC LIMITS OF MATERIALS FOR CONSTANT LOAD.

Material.	Specific Weight.		Breaking Stress.					
			Tension.		Compression.		Shear.	
	Lbs. per Cu. In.	Gms. per Cu. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.
Wrought iron	0.278	7.7	25.1	3.9	22.6	3.5	18.0	2.80
Cast iron	0.259	7.2	7.7	1.20	41	6.3	4.8	0.74
Cast steel	0.282	7.8	25.1	3.9	...	...	...	...
Forged steel	0.283	7.85	30	4.7	...	...	22.6	3.5
Copper (cast)	0.311	8.65	9.6	1.50	26.4	4.1	11.0	1.70
Copper (rolled plates)	0.317	8.8	14.2	2.20	...	...	18.0	2.80
Copper (hard drawn wire)	0.321	8.9	26.4	4.1	...	...	...	...
Brass (cast)	0.29	8.0	10.4	1.60	19.3	3.0	...	...
Gun metal	0.31	8.6	12.3	1.90	16.1	2.50	...	...
Phosphor bronze	0.321	8.9	26.4	4.1	50	7.7	19.3	3.0
Manganese bronze	0.31	8.7	29.5	4.5	...	...	...	...
Delta metal (cast)	0.31	8.6	16.1	2.50	...	...	...	...
Aluminium (cast)	0.094	2.6	8.1	1.25	...	...	...	...
Aluminium bronze	0.278	7.7	21.3	3.3	...	...	...	...
Silicon bronze	0.321	8.9	26.0 to 64	4.0 to 10.0	...	...	...	...
Manganese steel	0.282	7.8	104	16.0	...	...	...	...
Chrome bronze	0.321	8.9	29.0 to 70	4.5 to 11.0	...	...	...	...
Wood — oak	0.028	0.78	6.8	1.06	4.5	0.72	1.02 along the fibres	0.158 along the fibres
Wood — pine	0.024	0.66	2.96 to 5.9	0.46 to 0.91	1.55 to 3.2	0.24 to 0.49	0.284 along the fibres	0.044 along the fibres

TABLE 11. — (*Continued.*)

Material.	Elastic Limit.						Working Stress.					
	Tension.		Compression.		Shear.		Tension.		Compression.		Shear.	
	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.	Tons per Sq. In.	Tons per Sq. Cm.
Wrought iron	14.8	2.30	14.8	2.30	11.0	1.70	6.8	1.05	6.8	1.05	5.4	0.84
Cast iron	5.0	0.77	...	...	3.6	0.56	1.93	0.30	9.9	1.54	1.04	0.16
Cast steel	14.2	2.20	13.5	2.10	...	...	8.1	1.25	8.1	1.25	5.0	0.77
Forged steel	22.6	3.5	...	...	...	...	9.0	1.40	9.0	1.40	6.3	0.98
Copper (cast)	...	...	...	...	...	...	2.26	0.35	2.26	0.35	2.00	0.31
Copper (rolled plates)	...	...	...	...	...	...	2.70	0.42	1.80	0.28	1.35	0.21
Copper (hard drawn wire)	9.0	1.40	9.0	1.40	6.4	1.00	5.4	0.84	4.3	0.67	2.26	0.35
Brass (cast)	5.0	0.77	12.3	1.90	...	...	2.26	0.35	1.80	1.28	1.23	0.19
Gun metal	8.1	1.25	...	...	...	...	2.26	0.35	...	...	...	...
Phosphor bronze	9.6	1.50	9.0	1.40	...	...	5.0	0.77	5.0	0.77	3.6	0.56
Manganese bronze	...	...	...	...	...	...	5.9	0.91	5.9	0.91	4.1	0.64
Delta metal (cast)	...	...	...	...	...	...	3.2	0.49	...	...	...	...
Aluminium (cast)	...	...	...	...	...	...	1.60	0.25	...	...	...	...
Aluminium bronze	11.6	1.80	...	...	...	...	...	...	...	...	...	...
Silicon bronze	18.0 to 52	2.80 to 8.0	...	...	...	...	...	...	...	...	...	...
Manganese steel	...	...	...	...	...	...	...	...	...	...	...	...
Chrome bronze	...	...	...	...	...	...	...	...	...	...	...	...
Wood — oak	...	...	...	...	...	...	0.64	0.098	0.43	0.066	...	...
Wood — pine	...	...	...	...	...	...	0.41 to 0.81	0.063 to 0.126	0.22 to 0.45	0.034 to 0.070	0.041	0.0063

PART II — ALTERNATING CURRENT GENERATORS.

CHAPTER V.

PRESSURE REGULATION OF ALTERNATING CURRENT GENERATORS.

FOR the purpose of studying the influence of various factors on the regulation, and in calculating the regulation for most of the designs worked out, we shall in this treatise follow the method set forth in a paper in which one of the present authors collaborated, and which was read before the American Institute of Electrical Engineers* in 1905. We shall not enter in detail into the theoretical basis of this method, but shall give an outline sufficient for purposes of calculation.

The pressure regulation of an alternator is preferably specified as the percentage by which the terminal voltage rises when the load is decreased† from full load to no load without changing the speed or the field excitation. This may be termed the "inherent regulation" and is generally specified for full load at unity power factor, and for full load at 80 per cent power factor. The two values are termed respectively "The inherent regulation at unity power factor" and "the inherent regulation at a power factor of 0.8." The full load current at other than unity power factor is to be taken at the value corresponding to unity power factor; i.e., the kilovolt-amperes and not the kilowatts is taken as the basis of rating of alternators.

The customary values of the inherent regulation range from 4 per cent to 7 per cent for a power factor of 1.0 and from 15 per cent to 22 per cent for a power factor of 0.8. The British Engineering Standards Committee recommend 6 per cent on full non-inductive

* Proc. *American Institute of Electrical Engineers*, vol. xxiii, p. 291, "A Contribution to the Theory of the Regulation of Alternators," H. M. Hobart & F. Punga.

† This is the most common and the distinctly preferable specification, but an alternative occasionally employed is to specify the percentage *drop* in voltage when the load is increased from zero to full load, the excitation and speed being maintained constant.

load and 20 per cent on full inductive load with a power factor of 0.8, as maximum values.

It is sometimes useful to also specify the "excitation regulation," which is defined as the percentage increase in the field current to maintain normal terminal voltage when the load is increased from zero to full load. The excitation regulation ranges from 10% to 15% on non-inductive load, and from 25% to 35% on inductive load with a power factor of 0.8, for normal machines.

In pre-determining the pressure regulation of an alternator, the quantity requiring to be ascertained is the additional field ampere turns necessary at rated load to maintain the normal terminal voltage, and it is with the estimation of this quantity that pressure regulation calculations are chiefly concerned. These additional ampere turns consist of three components whose respective functions are:

I. To counteract the demagnetising ampere turns of the armature due to the current at the rated load.

II. To make up for the pressure drop due to the reactance and resistance of the armature winding.

III. To provide for the increase in magnetic leakage, brought about by the increased magnetomotive force, which increases the total flux in the magnet cores and hence the flux density and ampere turns required for the magnet cores and yoke.

I. The Demagnetising Ampere Turns.

The demagnetising component of the armature ampere turns is given by the expression

$$D = KB\,ni \sin \phi'$$

where D is expressed in ampere turns, and:

K = a coefficient depending on the ratio of pole arc to pole pitch.

B = the breadth factor of the winding.

ni = the armature ampere turns per pole for all the phases.

ϕ' = the angle between the pole centre and the position of the armature conductor when it carries maximum current. This may be termed the "internal phase angle."

1. The values of K are as follows:

Ratio of pole arc to pole pitch	0.5	0.6	0.7
Value of K	0.82	0.78	0.75

2. The values for B, the "breadth factor," depend on the distribution of the winding, and are given in Table 12.

TABLE 12.

BREADTH FACTOR OF ARMATURE COILS.

Percentage of Pole Pitch Covered by One Side of the Armature Coil.	Values for B, the "Breadth Factor."
20 per cent	0.99
33 per cent	0.95
40 per cent	0.94
50 per cent	0.90
60 per cent	0.86
80 per cent	0.76
100 per cent	0.64

Single-phase windings cover from 20 per cent to 100 per cent of the pole pitch according as they are concentrated or thoroughly distributed windings.

Two-phase windings generally cover 25 per cent of the pole pitch on either side of a coil of one phase, the whole coil thus covering one half of the pole pitch, and thus having a breadth factor of 0.90.

The commonest three-phase winding is the 2-range winding (which is half coiled, i.e. has one coil per phase per pair of poles, see p. 231, Chapter XI), and with this winding, a single coil of one phase covers one third of the pole pitch on either side, and thus has a breadth factor of 0.95. Three-phase distributed wave windings also spread 33 per cent of the pole pitch on either side of the winding of one phase under a pole.

3. The armature ampere turns per pole, ni, may be designated as the *armature strength;* the armature strength is in this treatise taken equal to the number of turns per pole for all the (n) phases multiplied by (i) the current in the windings, and is thus equal to $\frac{Tni}{p}$ where T = the total number of turns per phase, n = the number of phases, i = the current per phase, and p = the number of poles. Let us, for instance, consider a 3-phase 3000 kva. 8-pole, 11,000 volt, 50 cycle, Y-connected alternator having 120 slots and 4 conductors per slot. The total number of conductors on the armature is $120 \times 4 = 480$,

and the conductors per phase $= \frac{480}{3} = 160$. T, the number of turns per phase, is thus equal to $\frac{160}{2} = 80$. The voltage per phase equals,

$$\frac{11000}{\sqrt{3}} = 6350.$$

The current per phase at full load is equal to

$$\frac{3,000,000}{3 \times 6350} = 158 \text{ amperes.}$$

Hence for the armature strength we have

$$ni = \frac{Tni}{p} = \frac{80 \times 3 \times 158}{8} = 4740 \text{ ampere turns.}$$

4. The "internal phase angle" ϕ' consists of three components

$$\phi' = \phi + \alpha + \beta.$$

ϕ is the phase angle by which the current in the external circuit lags behind the terminal voltage. The power factor in the external circuit is equal to $\cos \phi$.

α is the phase angle between the internal E.M.F. and the terminal E.M.F. and due to the armature reactance.

β is the angular displacement of the magnetic centre of the field flux due to the distortion of the flux by the armature magnetomotive force.

The conditions in the alternator armature for a load having a power factor equal to $\cos \phi$, are as represented in the vector diagram of Fig. 57 in which

OV = the terminal voltage per phase.

OC = the current in the armature windings, (and of course also in the external circuit) lagging by an angle ϕ behind the terminal voltage OV.

$OP = RV$ = the reactance voltage in the armature when carrying the current OC.

RE = the voltage drop by ohmic resistance.

OE = the internal voltage per phase, generated in the armature by the flux crossing the gap.

ϕ = angle VOC = phase angle between current OC and terminal voltage per phase OV.

α = angle EOV = phase angle between internal E.M.F. per phase OE and terminal E.M.F. per phase OV.

For a load of unity power factor, ϕ = zero, and the conditions are as shown in Fig. 58.

Calculation of ϕ'. —The Internal Phase Angle.

Of the three components ϕ, α, and β:

(1.) ϕ is known from cos ϕ, the power factor.

(2.) *The determination of* α*:* To determine α it is necessary to know the value of the reactance voltage(V) of the armature, and to be precise, also the resistance drop in the armature windings, the latter, however, generally exerting a far less influence on the final results than is exerted by the reactance voltage.

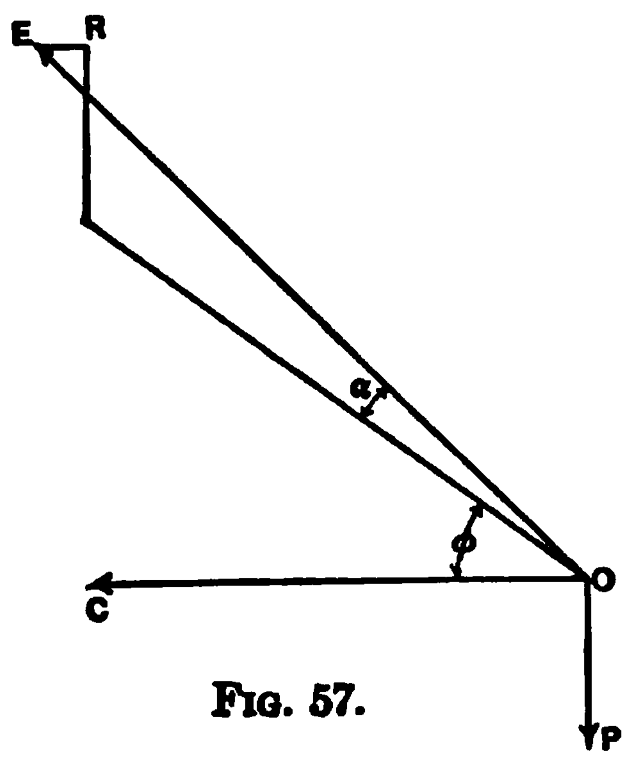


FIG. 57.

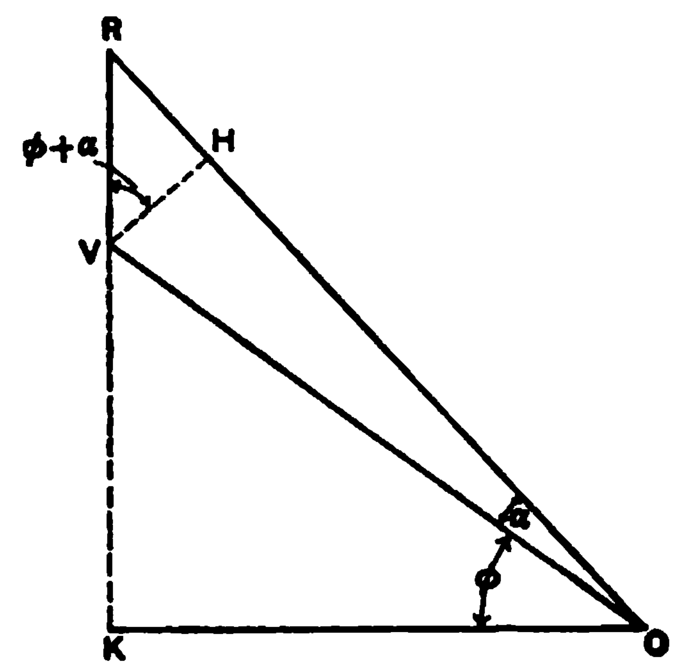


FIG. 57A.

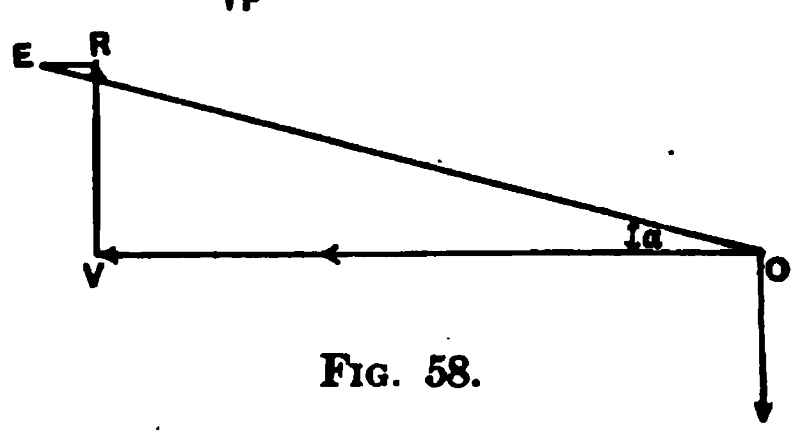


FIG. 58.

FIGS. 57, 57A and 58.—Vector diagrams relating to pressure regulation.

The reactance voltage is equal to the reactance of the armature, multiplied by the current, thus:

$$v = (2\pi Nl) \times i,$$

where

N = frequency in cycles per second.

l = inductance of the armature winding in henrys.

i = current in armature in amperes.

v = reactance voltage.

N and i being known, we can obtain v, the reactance voltage, as soon as we have determined l, the inductance.

Estimation of l, the Inductance of the Armature Windings in Henrys. — For ascertaining the inductance we require to know the number of lines linked with the armature conductors per ampere turn. With open straight-sided slots a sufficient approximation to the inductance of the embedded part may be obtained by an elementary magnetomotive force calculation, thus:

Let D = depth of slot in centimetres.
d = depth above top of winding in centimetres.
w = width of slot in centimetres.

Then the number of lines per ampere turn per centimetre of embedded length, for the case of the inductance of the winding in a single slot, may be taken as approximately equal to,

$$\frac{4\pi}{10} \frac{\frac{1}{2}(D-d)+d}{w} = 0.63\frac{D+d}{w}.$$

If the coil is spread over two slots, the length of the magnetic path through the air is doubled, and the number of lines per ampere turn per centimetre of core length is about one half the number for a single slot.

For the free length of the conductors, i.e., the parts not embedded in iron, the number of lines per ampere turn per centimetre may be taken as from 0.4 to 0.8.*

As a rough approximation we may estimate the inductance of the free length on the basis of the following figures:

Number of Slots per Pole per Phase.	Number of Lines per Ampere Turn per Centimetre of "Free" Length.
1	0.8
2	0.7
3	0.6
4	0.5

If we denote the total number of lines per ampere turn by Z, then we have for the 3000 kva. alternator which we have taken as example, Z = (free length in centimetres) $\times$ (0.5 to 0.8) + (the embedded length in centimetres) $\times$ (the lines per ampere turn per centimetre of embedded length).

* "Modern Commutating Dynamo Electric Machinery," H. M. Hobart, *Journal Institute Electrical Engineers*, vol. xxxi, p. 170, 1901.

The inductance of a coil having t turns is equal to $t^2 Z \times 10^{-8}$ and its reactance is equal to

$$2\pi N t^2 Z \times 10^{-8}.$$

The reactance per phase is the reactance per coil multiplied by the number of coils per phase. As an example let us return to the case of the 3000 kva. alternator mentioned on page 67.

A section of the slot is shown in Fig. 59.

Here $D = 6.0,$

$d = 1.2,$

and $w = 2.1.$

Consequently the number of c.g.s. lines per centimetre of embedded length is equal to

$$0.63\left(\frac{6.0 + 1.2}{2.1}\right) = 2.16.$$

The mean length of one turn = 516 centimetres.
"Embedded" length per turn = $2\lambda n$ = 190 centimetres.
"Free" length per turn = 356 centimetres.

The slots being open, no extra allowance is required for overhang of teeth. Hence the total number of lines per ampere turn per centimetre of embedded length $= 2.16$.

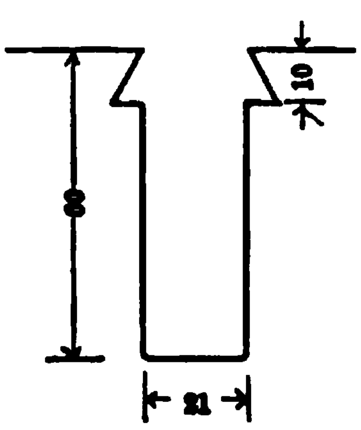


FIG. 59.
Slot of 3000 kva. alternator.

Spread of coil (*i.e.*, number of slots) = 5.

As there are 5 slots, the magnetic length of the leakage air path is approximately five times the magnetic length for one slot. The c.g.s. lines per ampere turn per centimetre of embedded length become approximately $\frac{2.16}{5} = 0.43$, say 0.5.

Hence we have:

c.g.s. lines per ampere turn for the free length $= 0.5 \times 356 = 178.$
c.g.s. lines per ampere turn for the embedded length $= 0.5 \times 190 = 95.$
c.g.s. lines per ampere turn $(Z) = 178 + 95 = 273.$
Number of turns per coil $(t) = 20.$

Inductance of one coil in henrys $(l) = t^2 \times Z \times 10^{-8} = 0.00109$ and the reactance of one coil $= 2\pi n l = 2\pi \times 50 \times 0.00109 = 0.342$

ohms. There are 4 coils per phase, therefore the reactance per phase is

$$0.342 \times 4 = 1.37 \text{ ohms}.$$

With full load current of 158 amperes, the reactance voltage per phase $= 158 \times 1.37 = 216$.

Expressed as a percentage of the terminal voltage per phase $\left(\frac{11{,}000}{\sqrt{3}} = 6350\right)$, this is equal to $\frac{216}{6350} \times 100 = 3.4$ per cent.

As the ohmic drop (ER of Fig. 57) is usually small compared with the reactance voltage and with the terminal voltage, we may neglect ER in estimating α. Consequently α is determined by the approximate relation

$$\sin \alpha = \frac{v}{V} \cos \phi,$$

when v = reactance voltage, and

V = terminal voltage.

This relation is apparent from the diagram in Fig. 57A in which the resistance drop ER has been neglected. The proof is as follows:

We have the angle $RVH = KOR = \phi + \alpha$,

also
$$\frac{VH}{RV} = \cos RVH = \cos (\phi + \alpha),$$

$$\therefore\ VH = RV \cos (\phi + \alpha),$$

and
$$\sin \alpha = \frac{VH}{OV} = \frac{RV}{OV} \cos (\phi + \alpha).$$

As α is exceedingly small compared with ϕ we may take $\cos (\phi + \alpha)$ as approximately equal to $\cos \phi$, and hence,

$$\sin \alpha = \frac{RV}{OV} \cos \phi = \frac{v}{V} \cos \phi \text{ approximately}.$$

α could of course be scaled off from a diagram such as Fig. 57, in which the various voltages are set out to scale, and there would then be no purpose in resorting to this approximation.

In the case we are considering, for full load at power factor 0.8, $\phi = 37$ degrees, the reactance voltage is 216, and hence,

$$\sin \alpha = \frac{216 \cos 37^\circ}{6350} = 0.0267,$$

whence $\alpha = 1.5$ degrees.

(3.) *The Determination of* β. We may determine the angle β from the distorting ampere turns. The equation for β, the degrees shift of the magnetic centre from midpole-face position is:—

$$\beta = \frac{\text{Arm. distort. amp. turns per pole}}{\text{Air gap ampere turns on field per pole.}} \times 90 \text{ degrees.}$$

Table 13 shows the expressions for the general case of distorting and demagnetising ampere turns.

TABLE 13.

EXPRESSION FOR DEMAGNETISING AND DISTORTING COMPONENTS OF ARMATURE AMPERE TURNS.

Ratio of Pole Arc to Pole Pitch.	Armature Ampere Turns (Effective) per Pole.		
	0.5.	0.6.	0.7.
Demagnetising ampere turns.	$0.82\, B\, ni \sin \phi'$	$0.78\, B\, ni \sin \phi'$	$0.75\, B\, ni \sin \phi'$
Distorting ampere turns. . .	$0.11\, B\, ni \cos \phi'$	$0.15\, B\, ni \cos \phi'$	$0.20\, B\, ni \cos \phi'$

In this table, ni denotes the effective armature ampere turns per pole, i.e., ni = (ampere turns per pole per phase) $\times$ (number of phases). B = the breadth factor already discussed on page 67.

The expression for the distorting ampere turns contains ϕ' which is equal to $\phi + \alpha + \beta$. The value of this expression is not known until β is determined. Hence in calculating the distorting ampere turns we must assume a preliminary value for β, and if the calculated value of β comes out sufficiently near the assumed value, the calculation will stand, but if not, then the new value should be substituted and the calculation revised.

For example, in the case of the 3000 kva. alternator we are considering, the ratio of the pole arc to the pole pitch is equal to 0.7, and the distorting ampere turns are ascertained from Table 13 to be:

$$0.20\, B\, ni \cos \phi'$$
$$B = 0.95 \text{ (see p. 67)}$$
$$ni = 4740 \text{ (see p. 68)}$$
$$\phi = 37 \text{ degrees (see p. 72)}$$
$$\alpha = 1.5 \text{ degrees (see p. 72).}$$

Assuming for β a trial value of 7 degrees, we have $\phi' = 37 + 1.5 + 7 = 45.5$ degrees and $\cos \phi' = 0.71$.

Hence the distorting ampere turns $0.20 \times 0.96 \times 4740 \times 0.71 = 6500$. The field ampere turns for the air gap for this design are 8100.

Whence $\beta = \frac{650}{8100} \times 90$ degrees $= 7.2$ degrees.

This value is so near the assumed value of 7 degrees that it is not necessary to re-calculate it, but if there were a wider difference, the value of $\beta = 7.2$ degrees should be substituted, giving $\phi' = 45.7$ and the calculation revised on this basis. We have now determined ϕ, α, and β, and from them, ϕ', from which latter quantity the demagnetising ampere turns may next be calculated by aid of the expressions in Table 13. For the case under consideration the demagnetising ampere turns for $\cos \phi = 0.8$ are

$$= 0.75\, B\, ni \sin \phi'$$
$$= 0.75 \times 0.95 \times 4740 \times 0.707$$
$$= \mathbf{2400} \quad \ldots\ldots\ldots\ldots \quad \text{(I)}$$

Thus we have finally arrived at the value of D, the demagnetising ampere turns, in the expression $D = KB\, ni \sin \phi'$ on p. 66.

II. The Ampere Turns required for the Reactance Voltage.

The second component of the additional field ampere turns is required to make up for the slight loss of pressure due to the reactance voltage. This component can be read off from the no load saturation curve. For the case under consideration, the no load saturation curve is given in Fig. 60. At unity power factor the component of the reactance voltage in phase with the terminal voltage is zero, and at any power factor, $\cos \phi$, the component is approximately $v \sin \phi$, as may be seen from Fig. 57. We have:

$$v \sin \phi = 216 \sin 37° = 130,$$

and from the no load saturation curve of Fig. 60 the ampere turns required for this voltage are found to be **280** . . . . (II)

The no load saturation curve can be readily drawn by working out the field ampere turns for the magnetic circuit at two voltages and drawing the curve through the two points obtained. One of the points should be worked out for the normal voltage of the machine and the other for a voltage some 20 per cent higher than normal voltage provided the flux densities are not too high on the saturation curves, as these curves are always unreliable at very high

densities. In estimating the 20 per cent higher point, all the flux densities throughout the magnetic circuit are multiplied by 1.2, and the corresponding ampere turns for each part are taken from the appropriate saturation curve for the material.

When high densities are employed, as so often occurs, especially in

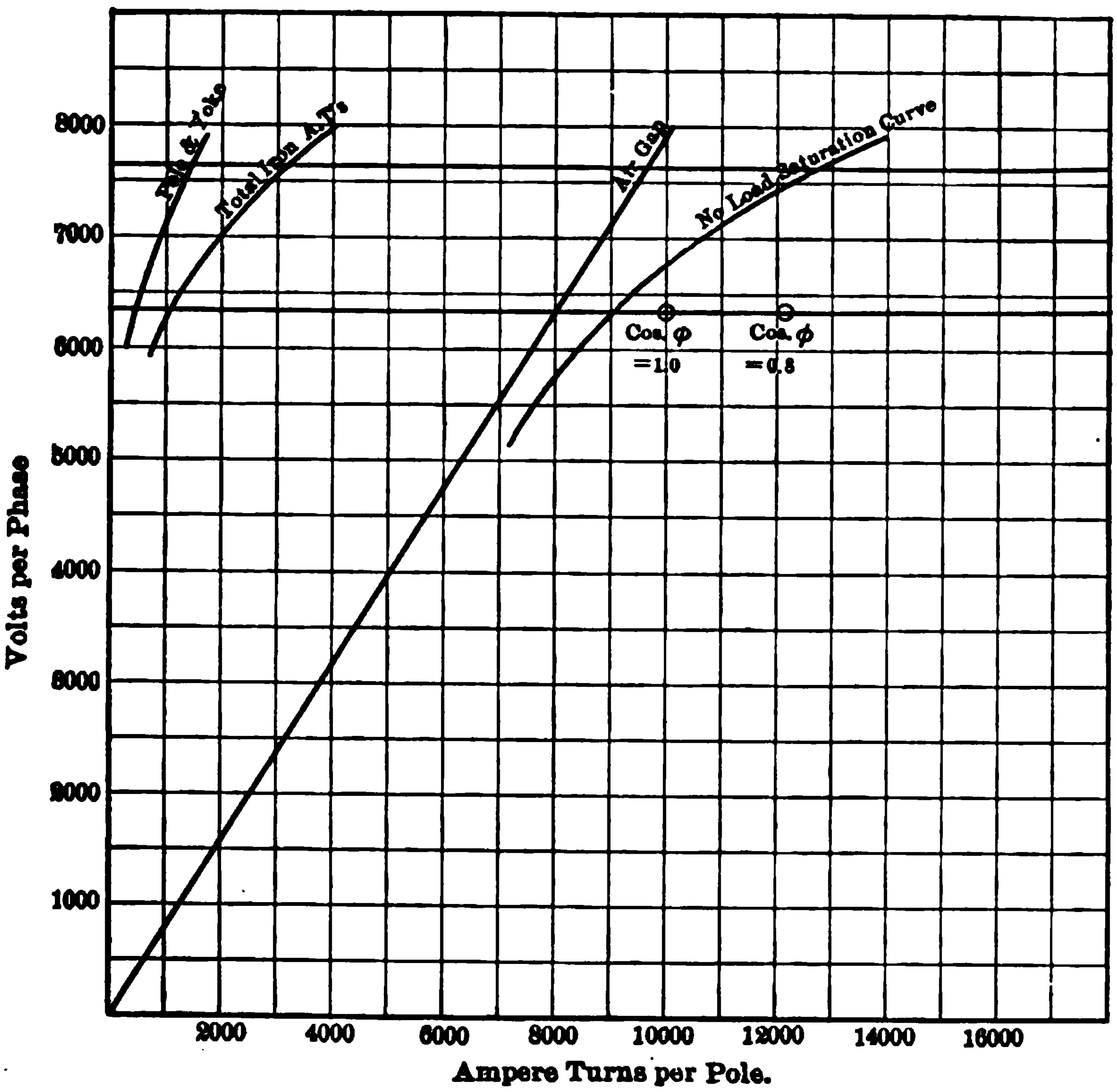


FIG. 60.— Saturation curves for 3000 kva. 3-phase 750 R.P.M. 8-pole 50 cycle 11,000 volt alternator.

the armature teeth, the upper parts of the saturation curves are not reliable, and the following rough rule will be useful:

For wrought iron, stampings, or iron or steel forgings, for densities above 16,000 lines per square centimetre, the percentage increase in ampere turns is from 8 to 10 times the percentage increase in flux density. Thus for a 20 per cent increase in density the increase in ampere turns will be 8 to 10 times 20 per cent, i.e. 160 per cent to 200 per cent, the higher value going with the higher saturations.

For example, let us take the armature teeth for the case in hand.

The no load flux density in the teeth for 6350 volts, is 18,000, and at 1.2 times normal voltage (= 7620 volts), the density is 1.2 × 18,000 = 21,600.

The ampere turns for teeth at normal voltage are 540, and at 7620 volts, will be 200 per cent greater, as the saturation is very high and hence the ampere turns are 3 × 540 = 1620.

III. The Ampere Turns for the increased Magnetic Leakage.

The third component of the additional ampere turns is required to make up for the increased magnetic leakage brought about by the higher magnetomotive force on the field magnets. An estimation of the leakage coefficient is consequently necessary.

$$\text{The leakage coefficient} = \frac{\text{useful flux} + \text{leakage flux}}{\text{useful flux}}.$$

The useful flux is the flux in the armature, and the useful flux + the leakage flux = the flux in the magnet cores and yoke.

The leakage flux is approximately equal to

$$C \times \frac{h\,(b + 15)}{a} \times \text{(Ampere Turns per Field Spool)}$$

for cores of rectangular cross section and

$$C \times \frac{h \times b}{a} \times \text{(Ampere Turns per Field Spool)}$$

for cores of circular cross section. The small letters represent the dimensions of the magnet cores as in Fig. 61. C is a factor lying between 2.4 and 3.4, the higher values applying in cases where the sides of the poles are close together and more nearly parallel with one another and where the pole tips are near together.

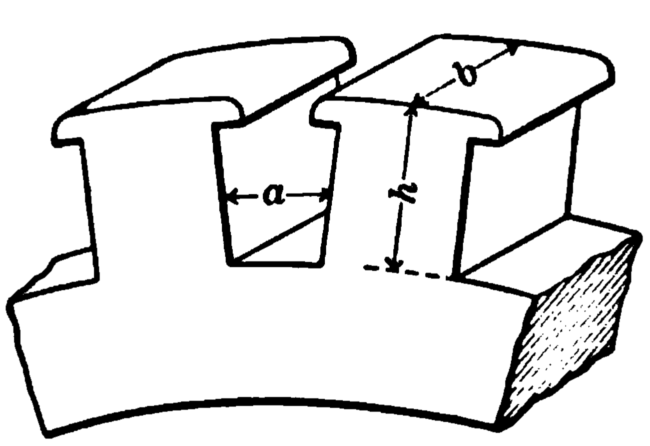


FIG. 61.—Alternator magnet cores.

In high speed machines with few poles which are far apart at the tips, the lower figure will apply. For the case through which we are working, we have $h = 27$, $a = 20$, $b = 140$.

No load ampere turns on field = 9200, whence leakage flux = 4,600,000.

The useful flux required in the armature per pole at unity power

factor is 34 megalines, and thus the leakage coefficient is equal to

$$\frac{34 + 4.6}{34} = 1.14.$$

At the excitation necessary for normal voltage at full load and cos $\phi = 0.8$ the leakage coefficient is considerably greater on account of the greater magnetomotive force on the field, and the leakage flux will be increased in proportion to the field excitation.

In the case under consideration, we have already determined the demagnetising ampere turns and the ampere turns for the reactance voltage. These come to **2400** + **280** = **2680.** As these constitute the bulk of the extra ampere turns, we may, for purposes of estimating the increased leakage, take the total ampere turns at full load and cos $\phi = 0.8$ as 9200 + 2680 = 11,880, and at this excitation the leakage coefficient will be

$$1 + 0.14\left(\frac{11{,}880}{9200}\right) = 1.18.$$

The extra flux, i.e., the leakage flux, only enters into the pole and yoke, and the densities in these parts only will be increased in the ratio $\frac{1.18}{1.14}$ (or 3.5 per cent increase) and extra magnetomotive force will only be required for the pole and yoke. Hence in Fig. 60 we have plotted the no load saturation curve for the poles and yoke separately.

The increase of 3.5 per cent in density, corresponds to a voltage increase of 3.5 per cent of 6350 = 222 volts, which from the saturation curve for pole and yoke, requires **300** extra ampere turns (III).

We have now the three components I, II, and III. The total extra field ampere turns are thus 2400 + 280 + 300 = 2980. The no load excitation is 9200 ampere turns, and thus the total field excitation required to maintain a normal terminal voltage of 6350 volts for rated load and cos $\phi = 0.8$, is 9200 + 2980 = 12,180 ampere turns. This gives us the extreme right hand point marked cos $\phi = 0.8$ on Fig. 60. This is a point on the saturation curve for full load at cos $\phi = 0.8$. The voltage corresponding to this excitation at no load is found from the no load saturation curve to be 7450 volts.

To this should be added the ohmic resistance drop which is $I \times R = 158 \times 0.126 = 20$ volts, bringing the total up to 7450 + 20 = 7470 volts. $\frac{7470}{6350} = 1.18$, consequently the inherent regulation at 0.8 power factor is equal to 18 per cent.

REGULATION CALCULATIONS FOR FULL LOAD AT UNITY POWER FACTOR.

At full load and unity power factor, the conditions are as represented in Fig. 58, page 69. We have $\phi = 0$ and $\phi' = \alpha + \beta$.

$$\alpha = \tan^{-1} \frac{216}{6350} = \tan^{-1} 0.034 = 2^\circ.$$

The reactance voltage remains equal to 216 as before, and the voltage per phase is equal to 6350.

The angle ϕ' will be small and for purposes of estimating β it will be sufficiently accurate to take $\cos \phi'$ as equal to 1.0, since for angles ranging from 0 to 20 degrees the variation in the cosine does not exceed 6 per cent.

The distorting ampere turns are now $0.20 \times 0.95 \times 4740 \times 1.0 = 900$ (see p. 73). The air gap ampere turns are 8100, whence,

$$\beta = \frac{900}{8100} \times 90^\circ = 10^\circ.$$

Hence we have $\phi = 2 + 10 = 12^\circ$.

The demagnetising ampere turns at unity power factor are

$0.75 \times 0.95 \times 4740 \times \sin 12^\circ$ (see p. 73)
$= 0.75 \times 0.95 \times 4740 \times 0.21$
$=$ **720 ampere turns (I).**

As the reactance voltage is in quadrature with the terminal voltage and consequently has no component in phase therewith, no extra field ampere turns are required to compensate for it. There will, however, still be a component required to provide for the increased leakage. This will amount to about 200 ampere turns and we must take it into account in estimating the increased leakage coefficient. The excitation at full load and unity power factor will be approximately

$$9200 + 720 + 200 = 10{,}120.$$

The increased leakage coefficient for this excitation will be approximately

$$1 + 0.14 \left(\frac{10{,}120}{9200}\right) = 1.155.$$

This gives an increase in flux density in the poles and yoke of $\frac{1 \cdot 155}{1 \cdot 14} = 1.5$ per cent, which corresponds to an increase in voltage of

$0.015 \times 6350 = 84$ volts. From the saturation curve for poles and yoke (Fig. 60) we find that this requires **150 extra ampere turns (III).**

The total extra field ampere turns are thus **I** + **III** = **720** + **150** = **870.** Adding this to the no load excitation of 9200 ampere turns we find that **10,070 ampere turns** are required to give full terminal voltage at full load and unity power factor. This point is marked on Fig. 60 and is a point on the full load, unity power factor, saturation curve. The corresponding voltage on the no load saturation curve for an excitation of 10,070 ampere turns is 6750 volts. $\frac{6750}{6350} = 1.063$. Consequently at full load and unity power factor the inherent regulation is 6.3 per cent.

The *IR* drop which is practically in phase with the terminal voltage and which amounts to 20 volts or 0.3 per cent, must be added on. The total inherent regulation for full load and unity power factor is thus 6.6 per cent, or say 7 per cent.

THE NO LOAD SATURATION CURVE AND THE SHORT-CIRCUIT CHARACTERISTIC.

The regulation is closely dependent on the degree of saturation of the iron parts of the magnetic circuit and on the short circuit characteristic.

As regards the former, it may be said that the more highly saturated the magnetic circuit the closer is the regulation. The regulation is in this case improved by virtually bending the saturation curve over, i.e., by working the iron of the magnetic circuit very near its saturation limits. This procedure is advantageous in that it minimises the cross section of the magnetic paths, and consequently the total weight of iron in the machine. Furthermore, by saturating the pole cores to a very high value, the mean length of one turn of the field winding is minimised, and consequently the weight of the field copper is reduced. It is a dangerous plan, however, since the material of the magnets (especially if cast) may turn out to be of inferior quality. Furthermore, should the machine be called upon to run at a voltage appreciably greater than the normal voltage, there will be difficulty, and perhaps impossibility, of getting sufficient flux through the machine for the high voltage, no matter how much the excitation is increased. The excitation regulation is also very great.

With regard to these points, it is best practice to keep within reasonable flux densities, desirable values for which are given in Table 21, page 113, Chapter VII.

It is not so much the actual ampere turns expended on the iron, but rather the proportion which they bear to the total field ampere turns, or the ratio of iron to air gap ampere turns, which is of chief importance. The use of extra long air gaps will not necessarily be accompanied by a corresponding improvement in regulation; it is rather a matter of balance between the magnetomotive force for the air gap and for the iron; and as a matter of fact, in average cases at normal voltage, the ratio of iron ampere turns to air gap ampere turns ranges from 1:5 up to 1:7, i.e., the air gap absorbs from 80 per cent to 90 per cent of the total ampere turns. Such proportions are typical of a normal design so far as the regulation and the value of the short circuit current are concerned.

The short circuit current in the armature at any given field excitation is of such a value that the armature reactions just counterbalance the field excitation. The field excitation is offset by the armature demagnetisation and by the reactance drop in the armature. The short circuit characteristic showing the relation between the field ampere turns with the armature current (i.e., the armature ampere turns) is approximately a straight line, and hence the short circuit current is practically proportional to the ratio of the field ampere turns per pole to the armature ampere turns per pole.

A "strong" armature is one in which, at rated load, the armature ampere turns are large in proportion to the field ampere turns. Conversely a "weak" armature is one in which, at rated load, the field ampere turns are large compared with the armature ampere turns.

The voltage regulation will of course be closer the less the armature demagnetisation, and as this depends for a given rating or current on the number of armature turns it may in general be said that the regulation with "strong" armatures is usually not so good as with "weak" armatures. Apart from the degree of saturation of the magnetic circuit, the regulation depends chiefly on the ratio of the field ampere turns to the armature strength. We have already seen that the fewer the poles the less is the proportion of the field ampere turns which can be expended on the iron parts, and consequently in these cases the field must be relatively stronger.

This is a point where we have two opposing tendencies in high speed alternator designs. A small number of poles is associated with high speeds, and in order to avoid excessive peripheral speeds, small diameters are necessary. This circumstance involves a very restricted amount of space available for the field copper, whereas on account of the few poles and low saturation component a large amount of field copper is required in this constrained space.

The outcome is either that the regulation is poor, or the field windings are liable to run excessively hot. These points are especially emphasised in the case of bipolar alternators, as will be noted from the test results for regulation of a 500 kva. bipolar alternator, which are recorded in Chapter VIII, page 152.

If the magnetic circuit is not saturated, the regulation can be considerably improved by increasing the radial depth of the air gap, as will be seen from Fig. 62. Since the iron saturation component is small, we may treat the saturation curve as a straight line.

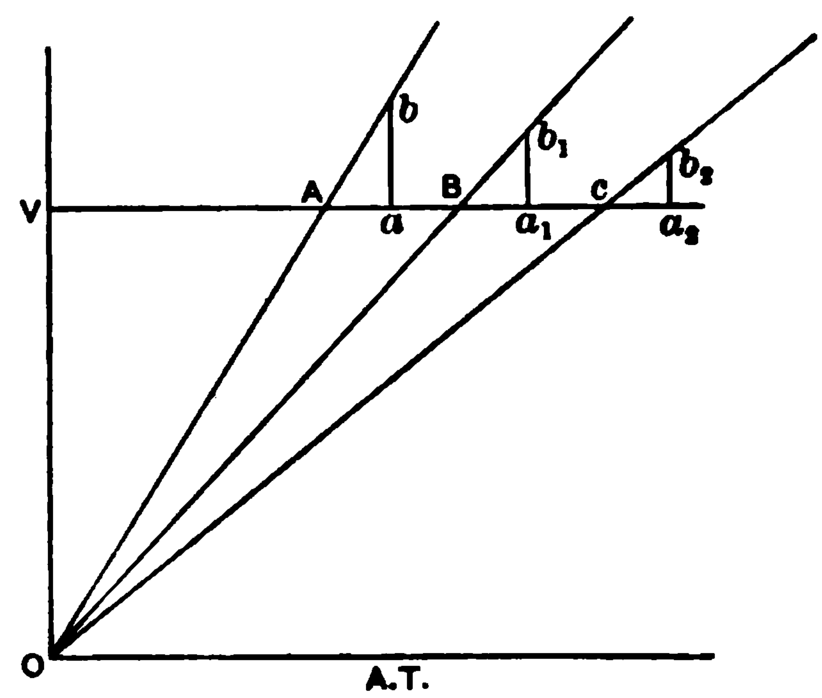


FIG. 62. — Straight line saturation curves.

1. Suppose with a certain length of gap, the excitation is VA for normal voltage, and that Aa is the armature demagnetisation. Then the regulation is $\frac{ab}{OV}$, which equals $\frac{Aa}{VA}$.

2. Now if the radial depth of gap is increased by 50 per cent, then the excitation is $VB = 1.5 \times VA$. For the same armature demagnetisation we have $Aa = Ba_1$, and the regulation is $\frac{a_1b_1}{OV} = \frac{Ba_1}{VB}$.

The regulation is thus equal to $\frac{Aa}{1.5 \times VA}$, which is 33 per cent less than in the first case.

3. If, now, the gap is increased by 100 per cent over its original value, then $VC = 2VA$, and for same demagnetising, $Ca_2 = Aa$, the regulation is

$$\frac{a_2b_2}{OV} = \frac{Ca_2}{VC} = \frac{Aa}{2VA},$$

which is one-half the value obtained in the first case.

The regulation is, for the three cases, proportional to the figures 100, 66, and 50 for air gaps of radial depths proportional to 1, 1.5, and 2, i.e., the product of regulation × length of gap is a constant. Hence, if there were no ampere turns expended on the iron in the circuit, the regulation would be inversely as the radial depth of air gap.

In turbo-alternators with few poles, especially with bipolars, the air gap often accounts for a proportion of the field ampere turns considerably greater than 90 per cent, and to such cases the above observations more pertinently apply.

If, however, the iron is saturated up to the highest practicable value, the machine saturation curve bends over rapidly and becomes nearly flat, and this feature renders the regulation more independent of the radial depth of the air gap.

The less the iron is saturated, the more will the regulation be improved by increasing the radial depth of the air gap (and the air gap ampere turns). Beyond a certain limit, however, any further increase in the air gap improves the regulation only slightly. This is illustrated in the curves of Figs. 63-65, which relate to a 6000 kva. 3-phase 6-pole 37.5 cycle 750 R.P.M. alternator, of which further data are given on page 201, Chapter X.

The excitation data for this machine as a normal design are as follows:

Radial depth of air gap	2.0 cms
Armature strength – ampere turns per pole for all phases	8500
Field ampere turns per pole at no load and normal voltage	16,600
Air gap ampere turns	14,400
Iron ampere turns	2200
Air gap ampere turns in per cent of total	87%
Iron ampere turns in per cent of total	13%
Ratio of field ampere turns to armature ampere turns	1.95
Short circuit current in terms of full load current	2.4
Field ampere turns at full load $\cos \phi = 1.0$	17,600
Field ampere turns at full load $\cos \phi = 0.8$	22,000
Voltage regulation at $\cos \phi = 1.0$	3.7%
Voltage regulation at $\cos \phi = 0.8$	16.4%

The no load saturation curve is drawn in Fig. 63 and is marked curve *D*. The corresponding field excitations for full load at power factors of 1.0 and 0.8 have been marked by points *D*. On the same sheet,

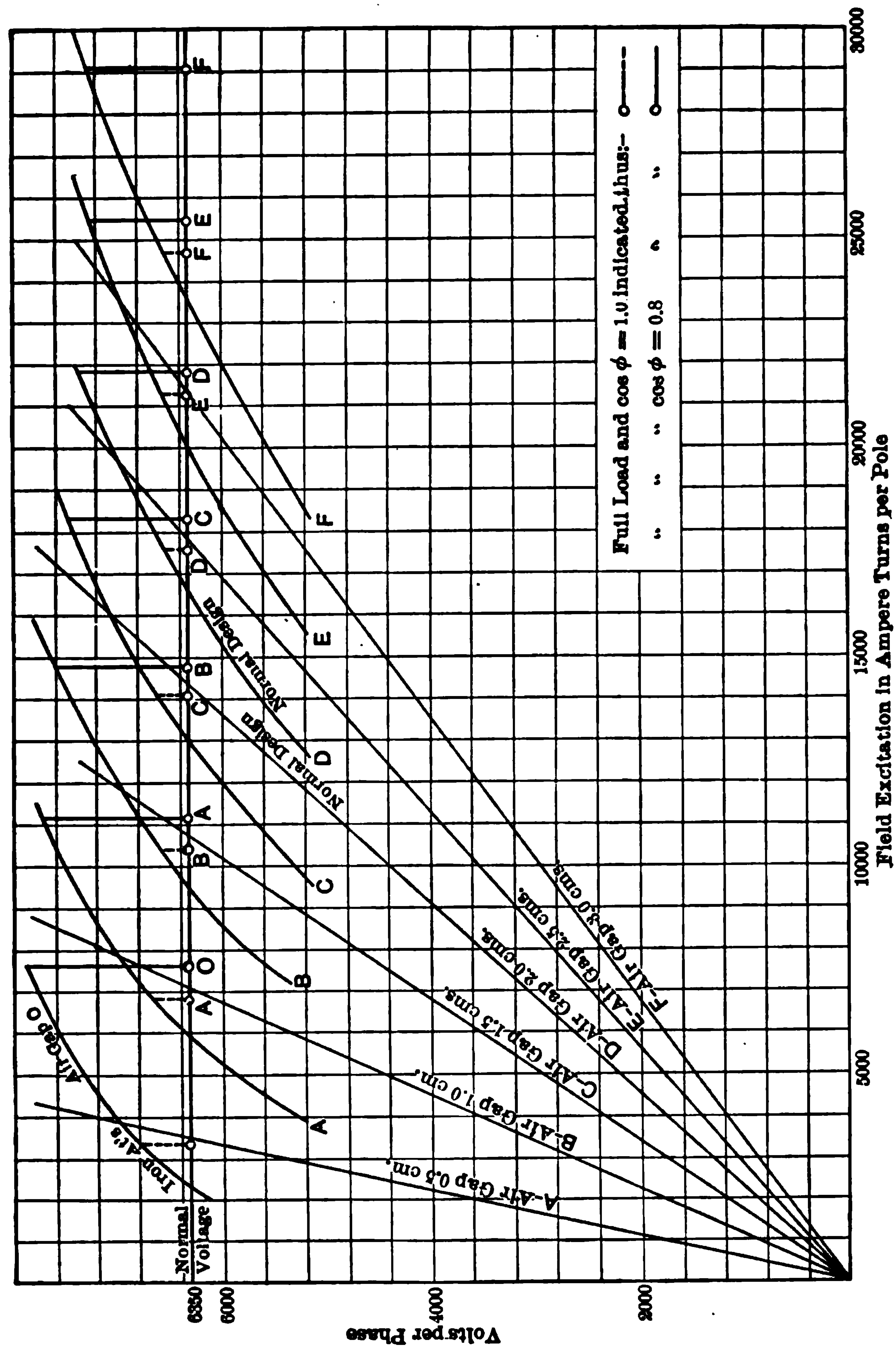


FIG. 63. — Saturation curves showing the effect of the radial depth of the air gap upon the regulation of a 6000 kva. 3-phase 6-pole 750 R.P.M. 37.5 cycle 11,000 volt alternating current generator.

similar curves and points are shown for various depths of air gap ranging from 0.5 centimetres to 3.0 centimetres, marked *A, B, C, D, E,* and *F.* The extra field ampere turns to counteract the armature reaction have been taken the same in each case, and adding these on to the no load ampere turns gives the corresponding points for full load excitation. The regulation is now readily obtained for

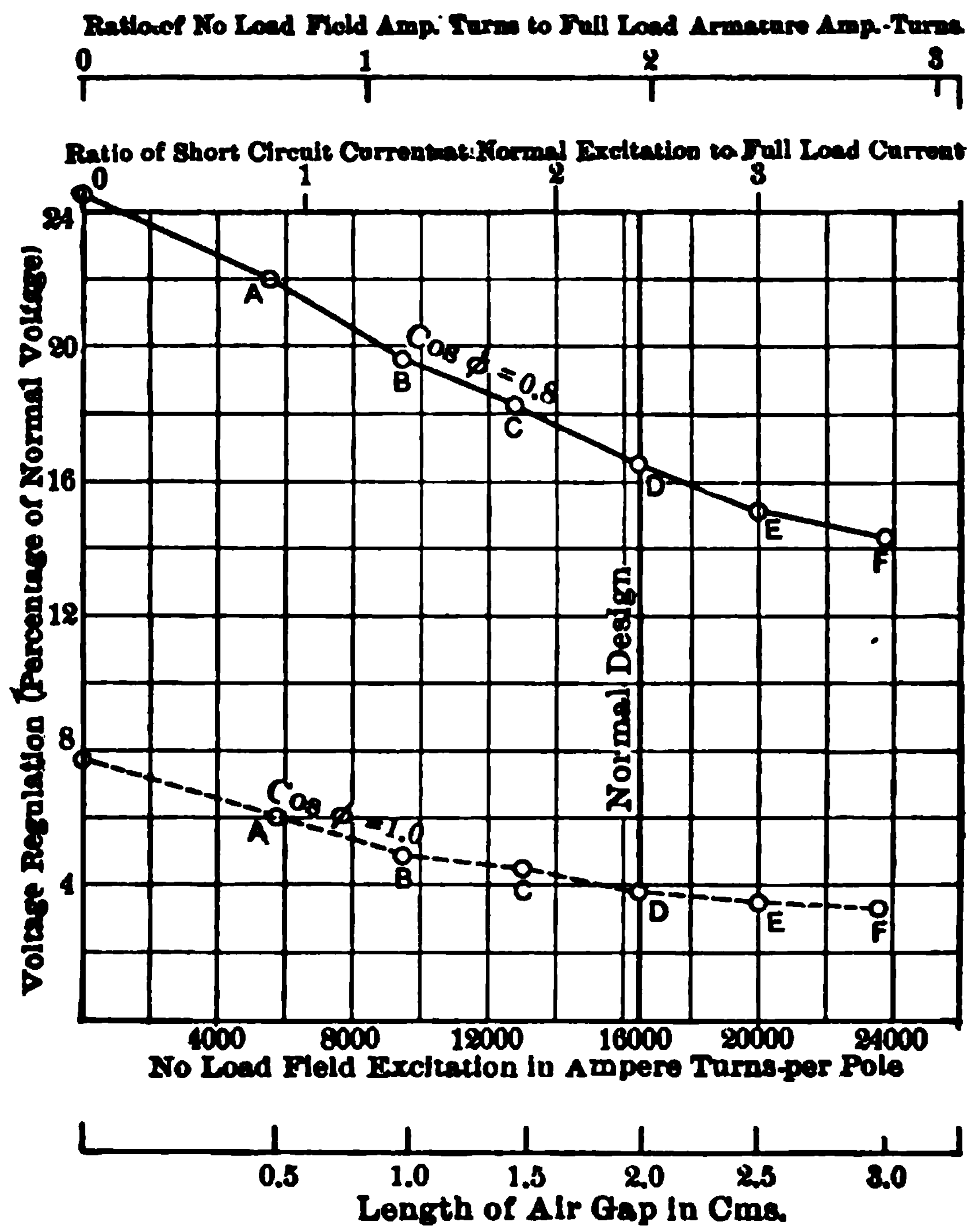


FIG. 64. — Curves showing relation between regulation and relative proportions of air gap and iron ampere turns for a 6000 kva. alternator.

each point, and corresponding to each of the different air gap depths.

In Table 14 are set forth the values of the regulation for each of the different air gap depths, and corresponding to the other quantities mentioned above.

These results are plotted in the curves of Figs. 64 and 65. Fig. 64 shows the regulation plotted against the no load field ampere turns, and the ratio of field to armature ampere turns. There are

also added scales for the length of air gap and the short circuit current which are obtained from the table above.

Beyond the line marked "normal design," which corresponds to the design as carried out, the improvement in regulation is but slight, as the curves are becoming flat.

In Fig. 65 the regulation is plotted against the air gap and iron ampere turns in per cent of the total field ampere turns. These percentages vary with the varying length of air gap. Curves as in Fig.

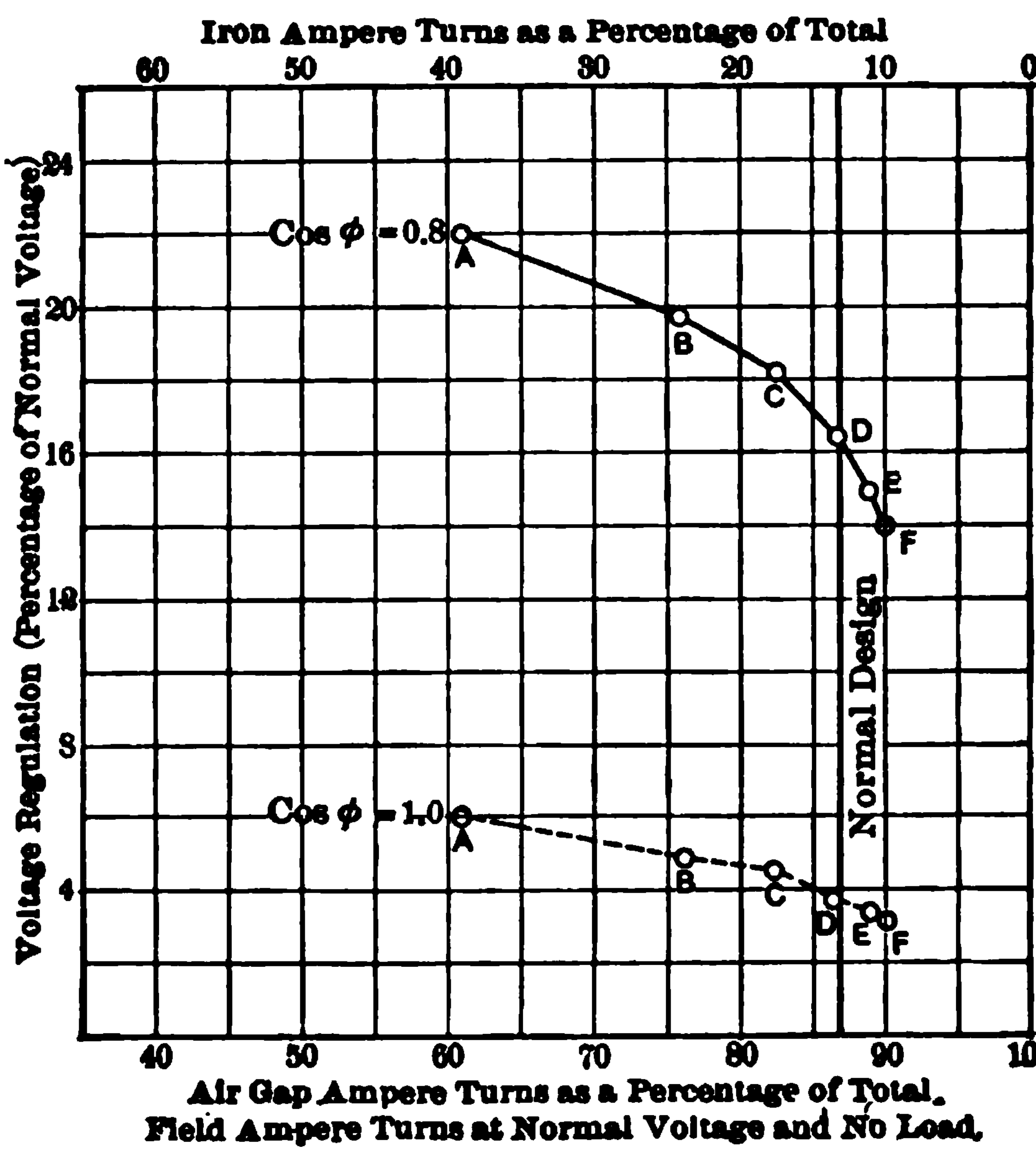


FIG. 65. — Curves showing relation between field excitation radial depth of air gap short circuit current and regulation for 6000 kva. alternator.

65 might be also plotted for a constant air gap depth and varying iron ampere turns, i.e., varying the degree of saturation of the magnetic circuit.

The range of values for the regulation is not wide, as the iron is fairly highly saturated, and the saturation curves are bending over. Thus doubling the radial depth of gap from 1 centimetre to 2 centimetres changes the regulation at 0.8 power factor only from 19.4 to 16.4, or a decrease of only 16 per cent.

Let us compare this with the case of Fig. 62, where there are supposed to be no ampere turns expended on the iron, and where, consequently, the saturation curve is a straight line. In this case, it was found that doubling the depth of the gap halved the regulation, i.e., decreased it by 50 per cent, which is a good contrast to the previous case as emphasising the effect of saturating the iron.

TABLE 14.

SHOWING VALUES OF EXCITATION AND REGULATION FOR DIFFERENT RADIAL DEPTHS OF AIR GAP, FOR 6000 KVA. ALTERNATOR.

Reference Letter.	Radial depth α Air Gap in Cms.	Air Gap Ampere Turns in Per Cent of Total.	Iron Ampere Turns in Per Cent of Total.	Ratio of Field Ampere Turns to Armature Ampere Turns.	Short Circuit Current in Terms of Full Load Current.	Full Load Field Ampere Turns ($\cos\phi = 1.0$).	Full Load Field Ampere Turns ($\cos\phi = 0.8$).	Voltage Regulation ($\cos\phi = 1.0$).	Voltage Regulation ($\cos\phi = 0.8$).
O	0	0	100%	0.26	0.33	3,200	7,600	7.5%	24 5%
A	0.5	62%	38%	0.7	0.9	6,850	11,200	6.0%	23.0%
B	1	77%	23%	1.1	1.4	10,400	[illegible]	4.7%	19.7%
C	1.5	83%	17%	1.5	1.9	14,000	18,400	4.4%	18.3%
D	2 0	87%	13%	1.95	2.4	17,600	22,000	3.7%	16.4%
E	2.5	89%	11%	2.35	[illegible]	21,100	25,500	3.45%	15.1%
F	3.0	91%	9%	2.8	3.48	24,700	29,100	3.2%	14.2%

CHAPTER VI.

GENERAL CONSIDERATIONS RELATING TO THE INFLUENCE OF THE RATED OUTPUT AND SPEED ON THE DESIGN OF ALTERNATING CURRENT GENERATORS.

THE general influence of the rated speed on the average value of the output coefficient, ξ, has been considered in Chapter II, and it was then stated that high speeds do not permit of such high values for ξ as may be employed in the design of low speed machines for the same rated output. The difference in ξ between a high and low speed design for a given rated output is, however, not great, and if for the moment we consider ξ to have a uniform value, it follows that for a given rating, the volume of the armature ($D^2\lambda g$) must be inversely proportional to the speed (R). Since the total weight of the machine is a function of (but not directly proportional to) $D^2\lambda g$, as we have seen in Chapter II, Figs. 15 and 16, and since, further, the Total Works Cost is roughly proportional to the total weight, it follows that the machine would be lighter and cheaper the higher the speed.

Let us, however, make a rough preliminary examination of the influence of the speed on the qualities of the machine. In the case of alternators the two quantities which chiefly define the degree of excellence of the machine are:

1. Temperature rise,
2. Pressure regulation.

With continuous current machines the chief considerations are

1. Temperature rise,
2. Commutation.

Values for these quantities, as also for the efficiency, are generally specified in any particular case. The practicable efficiency, however, is closely associated with the thermal limitations, and both of these are dependent on the losses in the machine. Thus the efficiency is generally brought within its specified limits when the prescribed temperature rise is not exceeded.

The subject of temperature rise and its estimation has been considered in Chapter III. For our present purposes we may take the temperature rise as depending roughly on two quantities — the watts lost in the machine per unit of surface at the air gap, and the watts lost per unit weight of the material in which the losses occur (i.e., the electromagnetically active or effective material), or per unit of weight of the total mass of the whole machine. In any one class of machine, the electromagnetically active material may be taken as constituting a fairly constant proportion of the total weight, and hence it matters but little when the comparisons are relative, whether we express the loss in terms of the watts per ton (or kg.) of effective material, or as the watts per ton (or kg.) of total weight of the machine.

We have seen that with high speed machines, the dimensions are relatively small, but that the total losses for customary values of the efficiency (since the efficiency is of about the same value for high and low machines) will be much the same for a machine of a given rated output, for any rated speed. Hence with high speed machines, the watts lost per unit of air gap surface and per unit weight of machine are much higher with a high speed than with a low speed machine. By way of illustration we may refer to the two alternators of which data are tabulated in parallel columns on pages 95–98 of this chapter.

For these two machines the following data illustrate the above statements:

Rated output in kva.	1375	1500
Rated speed in R.P.M.	**94**	**1000**
Efficiency at full load and with ($\cos\phi = 0.8$)	96%	96.6%
Total of all losses in the machines in watts	55,000	53,000
Weight of effective material — tons	30.6	9.2
Air gap surface in sq. dcms. ($\pi D\lambda g$)	770	392
Watts lost per sq. dcm. of air gap surface (reckoned on total losses)	71	135
Watts lost per kg. of effective material	1.8	5.8
Peripheral speed — metres per second	24.5	63
$D^2\lambda g$ (cubic metres)	8.0	0.9

The above comparison is for machines of practically the same rating, the one designed for direct connection to a slow speed engine running at 94 R.P.M., and the other designed to be coupled to a steam turbine at a speed of 1000 R.P.M.

The speeds of the two machines are about as 10 : 1, and the values of $D^2\lambda g$ are about as 1 : 9. The weight of effective material in the

low speed machine is about 3.3 times that of the high speed machine; the total losses are practically the same for both machines.

Thus, although the high speed machine has only some one-third as much effective material as the low speed machine, there is generated in it just about the same total amount of heat. This is a typical comparison between high and low speed machines. It is not desirable to increase the dimensions of the high speed machine, in order to decrease the losses per unit of weight and of radiating surface, as this procedure would lead to increased weight and cost, and increased mechanical stresses.

Thus, if we are to keep within the same limits of temperature rise in the two cases, the designs must possess features insuring that the thermal emissivity per unit of surface shall be proportional to the rated speed. The designer does not consciously set himself the problem in just this way, but it is nevertheless fairly expressed in these words.

It is for these reasons that high speed machines call for very liberal provision for ventilation, and great attention must be paid to systematic ventilating, as we shall see subsequently in considering the ventilating designs employed in various machines. Increase in the rated speed augments the difficulty in dissipating the heat due to the losses, and consequently there is need for providing very liberal means for ventilation. The problem is to a certain extent simplified owing to the increase in the velocity of the cooling air through the machine under the influence of the high centrifugal forces in high speed machines. As a case in point, the peripheral speeds of the two machines noted above are respectively 24.5 and 63 metres per second.

It will also be noted that the high speed machine is provided with 7 ventilating ducts, each with a width of 1.27 centimetres, while this particular low speed design is not provided with ducts. The ratio of the armature net core length λ_n to the gross core length λ_g is 76 per cent and 95 per cent in the two cases. Thus in the high speed machine about 16 per cent of the armature gross core length is taken up by ventilating spaces. The latter figure is, however, usually greatly exceeded in high speed machines.

Thus, so far as heating is concerned, high speeds are not necessarily detrimental. The second limitation is pressure regulation, to which we must allude in a general manner at this point. It has been dealt with at greater length in Chapter V. The inherent regulation of an alternator is defined as the percentage by which the terminal

voltage rises when the load is decreased from rated load to no load, the speed and the field excitation remaining constant at the normal value for rated load.

This rise in voltage is dependent on the number of extra ampere turns necessary on the field to maintain normal terminal voltage when the machine is operated at its rated load. As we have seen in Chapter V the total excitation at rated load is made up of three components.

The first provides a number of ampere turns in excess of the field ampere turns required at no load for normal terminal voltage. This number of ampere turns is required for offsetting the armature demagnetisation D, in order to maintain normal terminal voltage at rated load. The second component provides a further number of ampere turns sufficient to produce in the armature a voltage sufficient to offset the resistance drop in the armature winding and the component of the inductive drop which is in phase with the terminal voltage. The third component provides a number of ampere turns sufficient to offset the increased flux densities in the magnets due to increased leakage when the magnets are heavily excited.

Coming now to an examination of the extent to which the inherent regulation is a function of the rated speed, we have to consider the armature demagnetising ampere turns and their relative magnitude as compared with the field ampere turns. In high speed designs the number of poles is small, being fixed by the speed and frequency, and thus the pole pitch and the length of the pole arc are large. Hence the magnetic flux per pole is great; in fact, usually as great as can be got through the machine without resorting to excessive flux densities.* The largest dimensions available for the magnet core are determined by laying out the armature diameter and length, and these dimensions must be made as small as is consistent with the heating limit. This is not done from motives of economy in material, but rather from considerations of mechanical design.

Thus, in comparison with low speed machines, the number of armature turns per phase requisite to give the specified voltage are relatively small, and the armature strength is low. Hence the armature is of lower inductance, as also for the reason that the windings are highly subdivided and are distributed over a large polar pitch.

* The flux per pole in high speed alternators of large capacity runs into values as high as 50 megalines.

The number of slots per pole per phase is large in turbo-alternators, ranging from three to nine or more. This is in consequence of the small number of poles and of the desirability of keeping reasonable values for slot pitch and for width of slots and teeth. Hence the internal angle of lag of the current, due to armature inductance, is small. In cases where the field winding is distributed in slots, this internal angle of lag is further reduced, as the distorting effect exerted on the flux by the armature ampere turns is less marked.

As regards magnetic leakage, it may be said that the leakage coefficients for high speed machines are usually smaller than for low speed machines of the same rated output, as the sides of the poles are much farther apart in the former case, and consequently in this respect the high speed machine is better.

By way of illustration we have tabulated below some of the leading data for the two machines already mentioned:

Rated output in kva.	1375	1500
Speed in R.P.M.	94	1000
Number of poles	64	6
Flux per pole in megalines	4.06	18.4
Total flux crossing the gap from all poles	260	110
Armature strength in ampere turns per pole	2400	6500
Ampere conductors per centimetre of periphery	195	202
Number of slots per pole	3	9
Number of slots per pole per phase	1	3
Leakage coefficient (calculated)	1.23	1.15

From the above considerations it would seem that the use of high speeds should effect improvements in the regulating qualities, but these influences do not suffice to permit of designs with abnormally close regulation. Nor is such very close regulation justified in ordinary cases, as it would require a large expenditure for field copper, or for "weak" armatures, or both, and low output coefficients. Further, the "short circuit current" of such machines is large compared with the full load current, and they are consequently undesirable from the point of view of protection against heavy currents on the occurrence of short circuits.

From this preliminary survey with particular reference to considerations of temperature rise and regulation, it appears that high speeds are not unfavourable, and as the commutation limit is not involved, it would at first seem that each increase in the rated speed would result in a corresponding diminution in weight. Theoretically

this should be so; but we have studied the matter from a number of designs worked out in Chapters VIII, IX and X, and have shown very conclusively that beyond a certain moderately high speed, the consequences of a further increase in the rated speed are not favourable as regards either cost or quality.

This very interesting conclusion confirms that reached by Behrend,* who has investigated a number of 1000 kw. generators for various rated speeds. Behrend's curve is shown in Fig. 66, and indicates that the weight of a 1000 kw. 3-phase 25-cycle alternator has already passed the minimum value at a speed of 1000 R.P.M., and that the weight at 1500 R.P.M. is as much as that at 250 R.P.M.

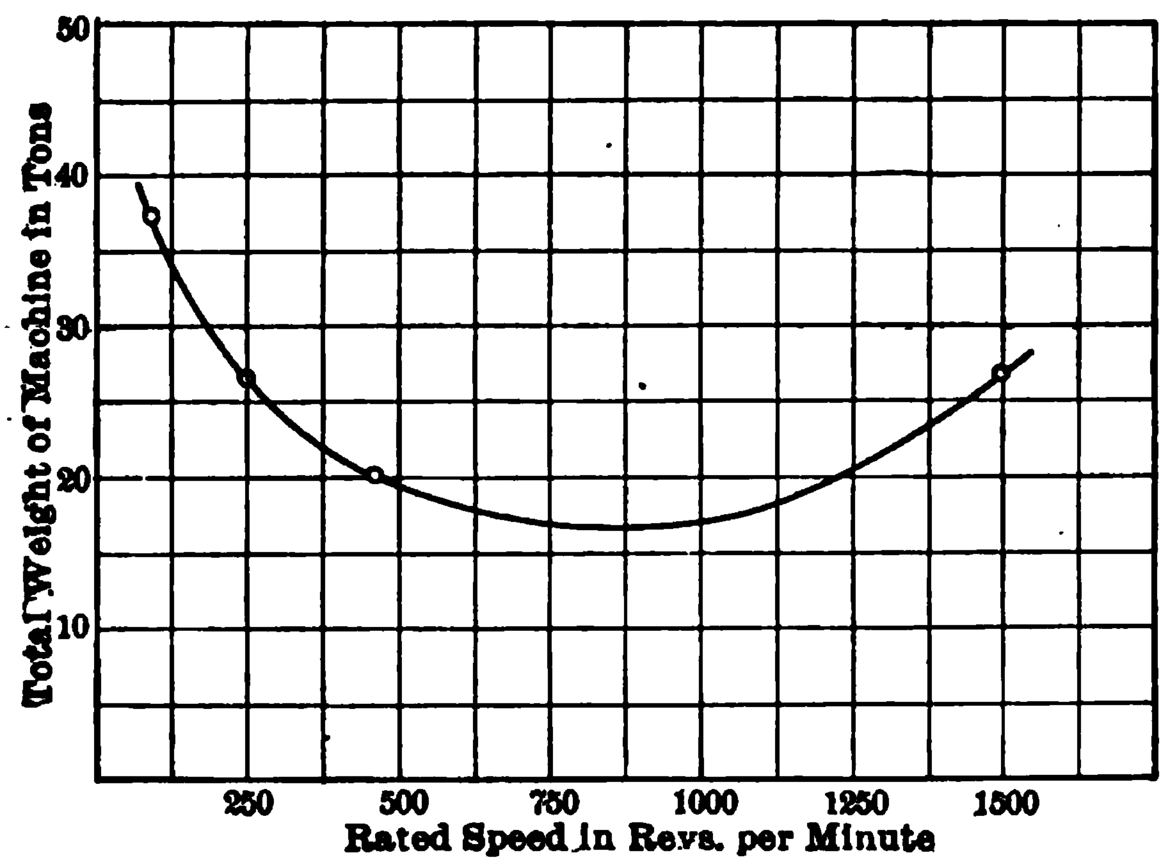


FIG. 66.— Behrend's curve, showing comparative weights of 1000 kw. 3-phase 25-cycle generators at different rated speeds.

This result related to several machines designed and built by the same firm.

The 1500 R.P.M. machine for 25 cycles has 2 poles, and we shall see in Chapter VIII that 2-pole designs are not so good as designs for equally high speeds but with a greater number of poles.

The problems arising in the design of a 2-pole machine are exceedingly difficult. The most economical speed for 1000 kw. appears from Fig. 66, to be 900 R.P.M., which is nearer the speed corresponding to a 4-pole design (750 R.P.M.), and leads to a much more favourable result than is practicable with two poles.

* *Electrical Review*, New York, Vol. 45, pp. 375–378 (1904).

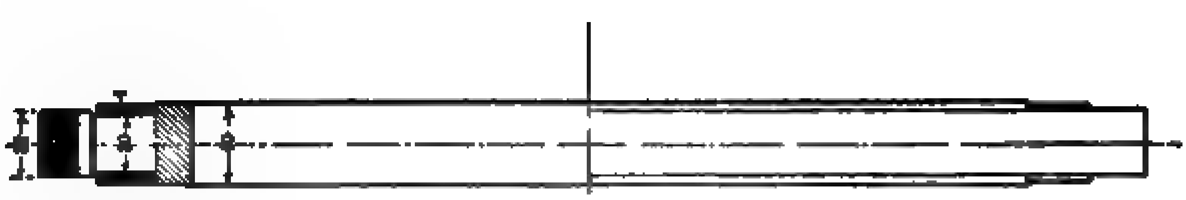


FIG. 68.

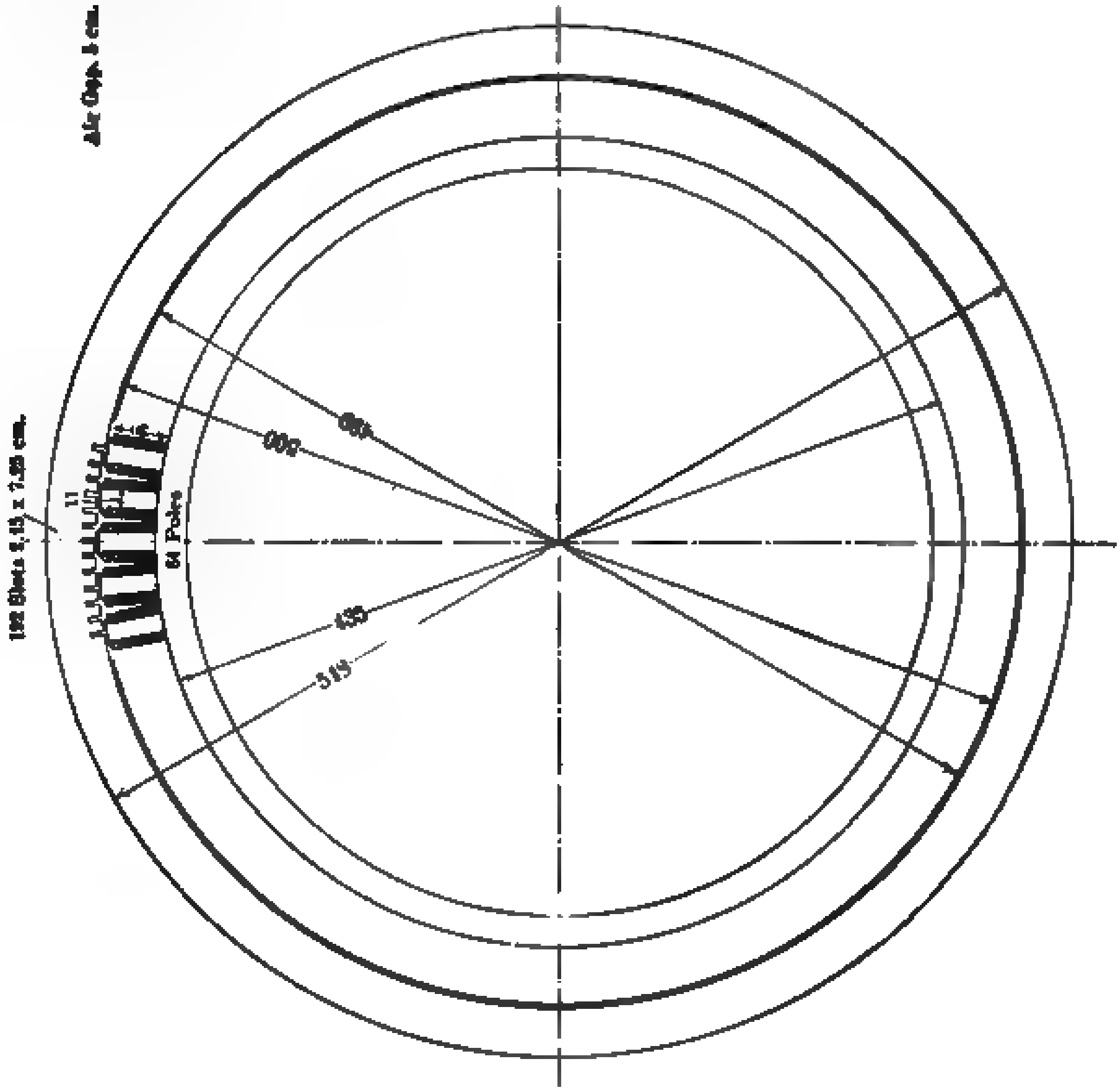


FIG. 67.

FIG. 67. — 1375 kva. 3-phase 64-pole 94 r.p.m. 50-cycle 5500-volt alternating current generator.
FIG. 68. — 1500 kva. 3-phase 6-pole 1000 r.p.m. 50-cycle 11,000-volt alternating current generator.

We thus see that the speed for minimum weight is considerably lower than the speeds of steam turbines of corresponding outputs (see Fig. 1 on p. 3); nevertheless, customary steam turbine speeds are much more favourable to alternators than to continuous current generators.

Behrend's curve also supports the statement made in the introductory chapter that ***"when not carried to excess the higher the speed in revolutions per minute, the more satisfactory will be the results which may be obtained in designing alternating current dynamo electric machinery."***

The following specifications for the two machines already referred to, afford a typical comparison in further respects as between a high speed and a low speed design for about the same rating. Drawings of these two machines are given in Figs. 67 and 68.

SPECIFICATION FOR TWO ALTERNATING CURRENT GENERATORS — LOW SPEED AND HIGH SPEED.

A.	1375 kva. 94 R. P.M.	3 phase 50 cycles	64 pole 5500 volt
B.	1500 kva. 1000 R.P.M.	3 phase 50 cycles	6 pole 11,000 volt

	A	B
Output in kva. (Power Factor = 1.0)	1375	1500
Terminal voltage	5500	11,000
Style of connection	Y	Y
Current per terminal	181	100
Speed R.P.M.	94	1000
Frequency	50	50
No. of poles	64	6

ARMATURE IRON

All dimensions in cms.

	A	B
Diameter at air gap (D)	500	122
External diameter of laminations	548	190
Gross length between core heads (λg)	32	58.2
No. of ventilating ducts	None	7
Width of each duct	None	1.27
Effective core length (iron) (λn)	28.8	44.5

SLOTS AND TEETH		
Total number of slots	192	54
Slot pitch at armature face	8.15	7.1
Width of slot	2.45	3.68
Width of tooth at armature face	5.7	3.42
Radial depth of slot	7.25	8
ARMATURE COPPER		
No. of slots per pole per phase	1	3
No. of conductors per slot	11	18
Section of conductor	0.454	0.303
Current density — amperes per sq. cm.	400	330
Dimensions of conductors bare	4 of 0.38 dia.	0.436 × 0.872
Number of turns in series per phase	352	162
ROTOR IRON		
Diameter at pole face	499.1	120
Length of air gap (mean)	0.45	1.45
Pole pitch at air gap	24.4	62.8
Ratio pole arc / pole pitch	0.57	0.66
Gross axial length (parallel to shaft)	30	55.2
Breadth of pole body across shaft	11	25.4
FIELD COPPER		
No. of turns per pole	54	300
Width of strip	...	2.54
Thickness of strip	...	0.09
Diameter of wire	1.1	...
Total cross section of winding space per pole	120	97.2
Length of winding space	30	16.2
Depth of winding space	4	6
Current density (amperes per sq. cm.)	134	236
MAGNETIC DATA		
Armature flux per pole	4.06	18.4
Leakage coefficient (calculated at no load)	1.23	1.15
Flux in the pole core	5.0	21.2
MAGNETIC DENSITIES		
Armature	4410	7900
Teeth	12,500	16,500
Pole core	15,100	15,800
Yoke	3560	12,600
At pole face	11,000	7900
Mean density in air gap	11,900	8100

LOSSES		
Armature Copper		
Mean length of turn	162	364
Resistance per phase at 60° C.	0.29	0.39
Total I^2R loss, $\cos\phi = 1$	18,000	7400
Total I^2R loss, $\cos\phi = 0.8$	28,000	11,600
Armature Iron		
Watts per kg.	2.15	6.85
Total core loss	12,300	30,500
Teeth		
Watts per kg.	17.2	30
Total tooth loss	3500	2080
Total iron loss	15,800	32,600
Iron loss + copper loss ($\cos\phi = 1$)	33,800	40,000
Iron loss + copper loss ($\cos\phi = 0.8$)	43,800	44,200
Watts per sq. cm. of armature surface $\cos\phi = 1$	44	102
Watts per sq. cm. of armature surface $\cos\phi = 0.8$	57	113
Field Copper		
Power for excitation at full load $\cos\phi = 1$.	6300	4500
Power for excitation at full load $\cos\phi = 0.8$.	11,000	8700
Watts per sq. dcm. of external surface of field spool $\cos\phi = 0.8$	5.4	75.5
EFFICIENCY		
$\cos\phi = 1$	97.2	97.1
$\cos\phi = 0.8$	96.0	96.6
REGULATION		
Inherent regulation at normal current and power factor = 1.0	5%	6%
At normal current and power factor = 0.8	14%	18%
CONSTANTS AND COEFFICIENTS		
Weight of effective material	30,570	9220
Weight of effective material — kgs. per kva.	22.2	6.15
Cost of effective material, dollars	21,421	6350
Cost of effective material — $ per kva.	3.9	1.06
$D^2\lambda g$ (cubic decimetres)	8.0	0.87
Output coefficient ξ	1.83	1.725
Ratio $\lambda g/\tau$	1.31	0.926

Constants and Coefficients — *Continued*		
Ratio $D/\lambda g$	17.1	3.28
Ampere conductors per cm. of periphery (α)	195	202
Flux per sq. cm. of armature surface (β)	5180	4940
Peripheral speed s	24.5	63
Watts per c.cm. of active belt	3.78	8.45
Ratio of field ampere turns to armature ampere turns	2.7	1.72
Kva. per pole	27	313
Weights in Kgs.		
Magnet cores	4900	1680
Magnet yoke	13,800	1230
Armature laminations	7800	5150
Effective iron, total	26,500	8060
Armature copper	890	450
Field copper	3200	710
Effective copper, total	4090	1160
Total weight of effective material	30,570	9220
Costs of Effective Material (dollars)		
Magnet cores	613	210
Magnet yoke	1725	155
Armature laminations	972	645
Effective iron, total	3310	1010
Armature copper	445	225
Field copper	1600	355
Effective copper, total	2045	580
Total cost of effective material	5355	1590

The kva. *per pole* for the low (A) and high (B) speed are respectively 27 and 313, and the "armature strengths" 2400 and 6500 ampere turns. The radial depths of the air gaps are roughly in proportion to the armature strengths, and are 0.45 and 1.48 centimetres respectively.

The output coefficient for A is rather greater than for B. The values of $D^2\lambda g$ are 8.0×10^6 and 0.9×10^6 respectively.

The analysis of the weights and costs of the active material shows that A contains 22.2 kgs. per kva. as against B's, 6.15. The total

weight of A is thus about 3½ times as great as that of B. The distribution of weight and cost among the individual parts of the machine does not differ widely in the two cases, as will be seen in the following table where the weights and costs of the iron and copper are shown as percentages of the total active material. The chief difference is in the disposition of the iron. In the slow speed generator, the armature laminations account for 25 per cent, and the magnet yoke for 45 per cent, while in the high speed generator the armature laminations account for 56 per cent, and the yoke for only 13 per cent.

TABLE 15.

PERCENTAGE WEIGHTS AND COSTS OF HIGH AND LOW SPEED ALTERNATORS.

	Weights.		Costs.	
Speed, R.P.M.	94	1000	94	1000
Magnet cores	16.0	18.2	11.4	13.3
Magnet yoke	45.0	13.2	32.3	9.7
Armature laminations	25.4	56.0	18.2	40.5
Effective iron—total	86.6	87.4	61.9	63.5
Armature copper	2.9	4.9	8.3	14.1
Field copper	10.5	7.7	29.8	22.4
Effective copper—total	13.4	12.6	38.1	36.5
Total effective material	100%	100%	100%	100%

It is evident from the drawings, Figs. 67 and 68, that this is due to the concentration of the yoke at the shaft and the great radial depth of stampings below the armature slots in the high speed generator. Apart from the effective material, it must be remembered that the low speed flywheel generator has a far larger amount of non-effective material in the large spider arms for the magnet wheel, and in metal for the stator frame.

The "weight factor" defined in Chapter II as the ratio of total weight of generator to weight of effective material is for these two cases 2.5 and 1.8, the total weights being 75 and 16.5 tons.

In addition to the material charges, there are also heavy labour charges associated with the large diameter of the flywheel generator.

By way of further illustration of the characteristic features of high speed alternators, there are brought together in Table 16 the leading data for 30 alternators of ratings ranging from 80 kva. to

TABLE 16.

LEADING DATA OF 30 ALTERNATORS OF VARIOUS SPEEDS AND RATINGS.

Reference Number.	Maker.	Rated Output, kva.	Terminal Volts.	No. of Phases.	Speed, R.P.M.	Frequency.	No. of Poles.	Line Amperes (Full Non-Induct. Load).	Armature Diameter, D cms.	Armature Core Length, λg cms.	Pole Pitch, τ cms.	$\frac{\lambda g}{\tau}$	$\frac{D}{\lambda g}$	$D\lambda g$.	$D^2\lambda g$.	Rad. Depth of Air Gap, mm.	Peripheral Speeds, metre per Second.	Output per Pole.	Output Coeff. $\xi = \frac{W}{D^2\lambda g \times R.P.M.}$
1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20
I	International Elec. Co.	80	530	3	600	50	10	87.	65	22	20.4	1.05	2.95	0.14	0.093	...	20.4	8	1.42
II	Witting, Eborall & Co.	100	2,400	1	770	90	14	41.	93	31	20.8	1.5	3.00	0.29	0.27	5.0	38	7	0.49
III	Johnson & Phillips	250	2,200	1	375	75	24	114	213	27.4	28	0.98	7.78	0.58	1.24	5.0	42	10.	0.53
IV	Oerlikon	250	...	3	3000	50	2	...	50	64.	78.5	0.815	0.78	0.32	0.16	8.0	81	125	0.47
V	Oerlikon	290	{2,080Δ {3,600Y	3	600	50	10	46.	90	40	28.3	1.4	2.25	0.36	0.32	2.5	28.3	29	1.50
VI	Kolben	300	...	...	3000	50	2	...	45.8	63.	72	0.87	0.72	0.29	0.13	12.0	72	150	0.75
VII	Messrs. Parsons & Co.	350	...	...	3000	100	4	...	43	128	33.8	3.8	0.33	0.55	0.23	...	67.5	87	0.50
VIII	Oerlikon	375	3,600	3	500	50	12	60.	139	140	36.4	3.85	0.99	1.95	2.7	...	36.4	33	2.80
IX	Westinghouse	400	...	...	3600	60	2	...	60	70	94	0.745	0.85	0.42	0.25	...	113	200	0.44
X	Brown, Boveri & Co.	500	550	3	3000	50	2	525	56.5	66	88.6	0.745	0.85	0.37	0.21	...	88	200	0.625
XI	Scott & Mountain	587	550	3	1500	50	4	525	114.4	68.6	51.3	1.39	1.58	0.78	0.90	15.0	89	125	0.37
XII	Schuckert	1000	...	...	1500	50	4	...	105	50	82.5	0.61	2.10	0.53	0.55	...	82.5	250	1.20
XIII	Oerlikon	1000	5,200	1	1500	50	4	192.	90	104	70.6	1.47	.86	0.93	0.97	10.0	71	250	0.80
XIV	Oerlikon	1200	2,000	3	1500	50	4	346	86	85	67.6	1.26	1.01	0.73	0.63	5.0	67.5	300	1.27
XV	Union F. G.	1400	...	...	500	50	12	...	160	70	42	1.67	2.29	1.12	1.80	13.0	42	117	1.56
XVI	G. E. Co. (N.Y.)	1500	...	...	1000	50	6	...	117	55	61.2	0.90	2.13	0.64	0.75	...	61	250	2.0
XVII	B. T. H. (Curtis)	1500	11,000	3	1000	50	6	79.	122	58.5	64	0.915	2.09	0.71	0.87	...	64	250	1.70
XVIII	British Westinghouse	1800	{4,630Δ {8,000Y	3	1500	50	4	130	127	81	100	0.81	1.57	1.03	1.30	32.0	100	450	0.92
XIX	B. T. H.	1880	11,000	3	1000	50	6	99	122	58.5	64	0.91	2.09	0.71	0.87	...	64	313	2.10
XX	Cie. Elec. & Mech. Geneva	2000	6,000	3	750	50	8	192	147.6	150	58	2.6	0.98	2.22	3.27	7.0	58	250	0.82
XXI	Union E. G.	2600	11,000	3	315	42	16	137	270	66	53	1.25	4.10	1.78	4.80	...	44.5	162	1.72
XXII	Messrs. Parsons & Co.	3500	{2,887Δ {5,000Y	3	1200	40	4	400	137.5	150	108	1.39	0.91	2.06	2.84	...	86.5	875	1.02
XXIII	G. E. Co. (N.Y.)	3750	2,200	2	250	25	12	850	336	61	88	0.69	5.5	2.06	6.90	8.0	42.5	312	2.20
XXIV	Oerlikon	3750	3,500	3	300	50	20	615	248	75	39	1.92	3.31	1.86	4.60	5.0	39	187	2.70
XXV	G. E. Co. (N.Y.)	5000	...	3	500	25	6	...	220	120	115	1.04	1.83	2.64	5.83	...	57.5	833	1.73
XXVI	B. T. H.	5000	9,000	3	500	25	6	320	238	127	125	1.02	1.87	3.02	7.20	...	62	833	1.40
XXVII	Westinghouse	5500	...	3	1000	33	4	...	171.5	147	134.5	1.09	1.17	2.52	4.32	82.5	90	1375	1.27
XXVIII	G. E. Co. (N.Y.)	7500	12,000	3	250	25	12	360	380	130	99.5	1.31	2.92	4.95	19.0	23.0	50	625	1.60
XXIX	G. E. Co. (N.Y.)	7500	{6,940Δ {12,000Y	3	250	25	12	360	380	130	99.5	1.31	2.92	4.95	19.0	19.2	50	625	1.60
XXX	G. E. Co. (N.Y.)	7500	6,600	3	375	25	8	660	...	...	...	...	...	...	...	...	...	...	...

7500 kva., and with speeds ranging from 3000 R.P.M. down to 300 R.P.M., speeds toward the lower limit having been included as typical of designs for machines driven by water turbines.

Excluding the machines of the lowest speeds, it is seen that the number of poles ranges from 2 to 8; in consequence, the length of the pole pitch reaches values as high as 100 centimetres, and in the case of No. XXVIII, 135 centimetres, as compared with the range of 15 to 30 centimetres which is usual for the pole pitch of very low speed machines. The peripheral speeds average 60 metres per second (being in one case as high as 113 metres per second) as compared with 10 to 30 metres per second for slow speed machines.

The output per pole is a conception which gives some ideas of the amount of field copper per pole, and its value has been entered in the table, where it is seen to amount to 1375 kva. per pole in the case of No. XXVII. This value, in the case of alternating current generators, of course depends on the rated output, but in comparing machines of a given rating for different speeds and numbers of poles, it is of considerable interest.

The radial depth of the air gap is entered in column 17. The air gaps are deep with machines of large output per pole. The deepest air gap shown is for the 5500 kw. 4-pole Westinghouse machine No. XXVII, which has a gap of 8.25 centimetres ($3\frac{1}{4}$ inches).

The output coefficient,

$$\xi = \frac{\text{Watts output}}{D^2\lambda g\ (\text{R.P.M.})},$$

is entered in column 20. A discussion on this coefficient has been given in Chapter II. It will be seen from Table 16 that the average values for the coefficient are, on the whole, considerably lower than the values obtained with low speed designs. This signifies that although the armature is doing greater duty from the standpoint of watts output per unit of $D^2\lambda g$, its *specific* duty, i.e., the watts per unit of $D^2\lambda g$ *per revolution per minute*, is not so high for the high speed ratings.

The ratio of the armature diameter to armature core length is shown in column 14. This ratio is not of great utility as a factor in design, but it conveys an idea of the contour and proportions of the armature. Thus, whereas in low speed designs the diameter is from 7 to 20 times the length, in high speed designs the average ratio lies between 1 and 3. The ratio of armature length to pole pitch

$\lambda g/\tau$ is of greater significance as a designing conception. Its value is fairly uniform at about 1.6, and, leaving out of account exceptional cases, it may be said to lie between 0.7 and 1.5.

The comparisons set forth in this chapter illustrate fairly well the general comparison between designs within the range of low rated speeds to those within the range of high rated speeds.

It is evident that, comparing two designs for a given rated output, one for a low speed and the other for a high speed, although the high speed effects considerable economy in the material, the economy is by no means proportional to the rated speed. It has already been stated that as one approaches, for a design of given rated output, the range of high speeds, the influence of, and the economy effected by, the rated speed become less marked. There is a certain speed most suitable for each rated output and frequency beyond which no further economy is effected by further increase in the rated speed.

It is the purpose of the immediately following chapters to examine these relations quantitatively and in further detail by the working out and study of a number of designs for alternating current generators.

CHAPTER VII.

GENERAL PROCEDURE IN ALTERNATOR DESIGN.

A PRELIMINARY design may be worked out from the basis of any of the design coefficients given in Chapter II. The most convenient and most extensively used of these is the output coefficient ξ. For a specified rated output, speed, and frequency, we can choose an appropriate trial value of ξ from the curves in Figs. 3 to 6 of Chapter II. This value determines the value of $D^2\lambda g$. To obtain the values of the two components of this term $D^2\lambda g$, we are partly controlled by the values of the ratios $\frac{D}{\lambda g}$ and $\frac{\lambda g}{\tau}$, where τ denotes the pole pitch at the air gap in centimetres.

The ratio $\frac{D}{\lambda g}$ lies between 0.6 for machines of large output and very high speeds, and 12.0 for designs for large output and very slow speeds. The average value of $\frac{D}{\lambda g}$ for machines up to 3000 kva. rated output and for steam turbine speeds is of the general order of unity, *i.e.*, the diameter is more or less equal to the gross core length.

The ratio $\frac{\lambda g}{\tau}$ is much more uniform than the ratio $\frac{D}{\lambda g}$, and usually has a value somewhere near unity, rarely exceeding 2.0 and rarely being less than 0.6. It is in the interests of a good design from the electromagnetic and thermal standpoint, to employ a fairly low value for $\frac{\lambda g}{\tau}$, but in high speed machines, high values of this ratio are unavoidable in view of mechanical requirements. The selection of the preferable value for this ratio should only be made after carrying out a considerable number of preliminary estimates. It is often useful to proceed from the assumption of a preliminary value for the pole pitch τ, as this has quite uniform values for low speed machines. This is a consequence of the fact that low speed machines have generally been designed with much the same peripheral speed. Twenty metres per second has been a frequently employed value.

The pole pitch τ in centimetres and the peripheral speed s in metres per second are related as follows:

$$s = 0.02\,\tau \times N$$

where N denotes the frequency in cycles per second.

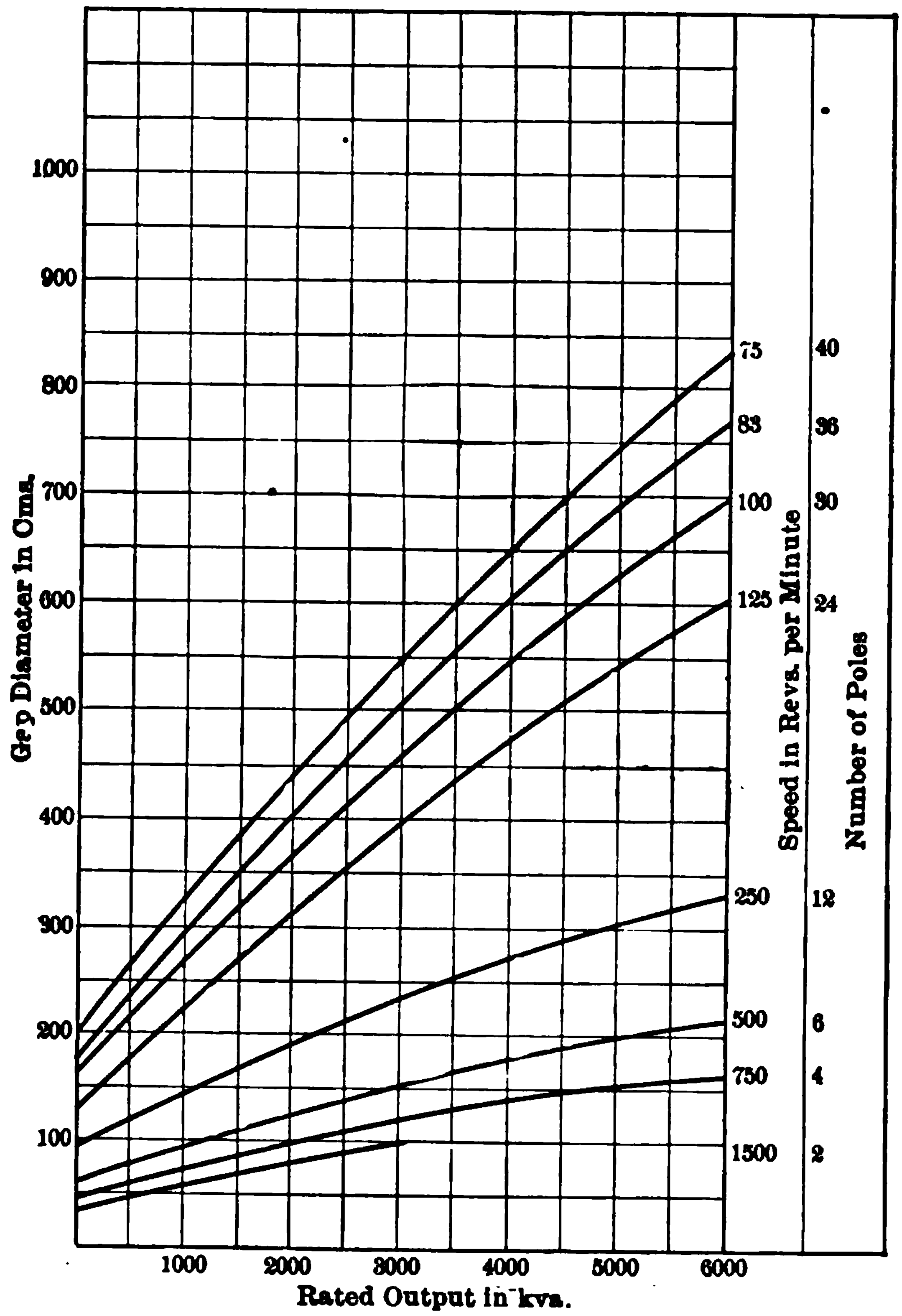


FIG. 69. — Curves showing air gap diameter for 25-cycle polyphase alternators as a function of the rated output for various speeds.

Hence, if $v = 20$ metres per second,

$$\tau = \frac{100 \times 20}{2 \times N} = \frac{1000}{N}.$$

For 50-cycle slow speed machines, 20 centimetres is an average value for the pole pitch, and for 25 cycles, some 40 centimetres. With low

speed machines of the customary well known type with internal revolving field, a fairly good basis has been to assume τ as from 20 to 30 centimetres for 50-cycle machines, and from 35 to 45 centimetres for 25-cycle machines.

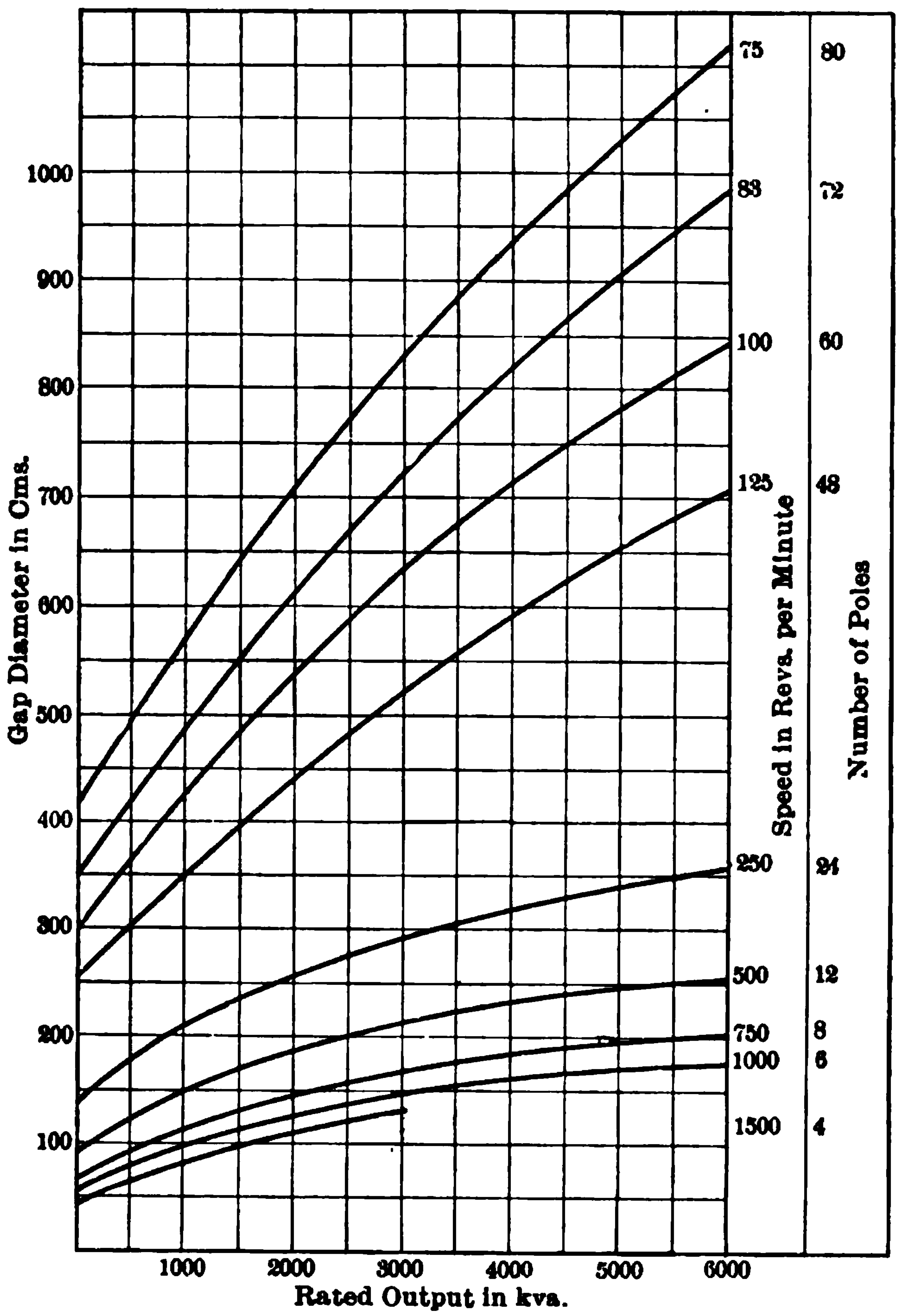


FIG. 70 — Curves showing air gap diameter for 50-cycle polyphase alternators as a function of the rated output for various speeds.

With high speed machines, where the peripheral speed varies from 30 to 100 metres per second, this basis is useless. In setting out a design for a turbine driven machine we do not at the outset assign any value to the peripheral speed to which to work, but this largely determines itself from the diameter ultimately arrived at as giving the best

design. Further, the peripheral speed corresponding to preferable dimensions is lower the lower the rated output. An experienced designer is able to decide in advance and with a considerable degree of correctness the preferable leading dimensions for a given case.

In Figs. 69 and 70 are given sets of curves which are useful as a rough basis for the predetermination of the diameter of a well proportioned design for a given rating. Fig. 69 relates to 25-cycle and Fig. 70 to 50-cycle alternators.

The curves give the air gap diameter plotted as a function of the rated output in kva., for various speeds from 80 R.P.M. to 1500 R.P.M. The number of poles corresponding to each speed is entered at the right hand side of each set of curves. With the value of ξ already chosen, and the value of D obtained from these curves, we can now proceed further with the design.

Having fixed the main dimensions, D and λg, we have next to determine either the number of turns on the armature or else the flux per pole, in order to carry the design further. α, the ampere conductors per centimetre of armature periphery, usually lies somewhere near 200; and the mean flux density at the air gap, β, is of the order of 6000 lines per square centimetre. As the output coefficient ξ is proportional to the product $\alpha\beta$, values for the latter must be in accordance with the value of ξ originally taken. If ξ is high, either or both α and β may be high, and vice versa. We obtain from these values a preliminary value for either the armature turns or the flux. The order of procedure is as follows:

1. The air gap circumference $= \pi D$.

The total number of ampere conductors around the gap periphery $= \pi D\alpha$.

If the current per phase is I amperes, the number of conductors on the armature is $\frac{\pi D\alpha}{I}$, and the number of conductors per phase $\frac{\pi D\alpha}{In} \cdot$ T, the turns per phase, $= \frac{\pi D\alpha}{2\,In}$.

Taking $$\alpha = 200,$$

then $$T = \frac{314\,D}{In} \qquad (1)$$

The flux per pole in *c.g.s.* lines is given by the formula

$$M = \frac{V}{kTN} \times 10^{-8}$$

where all the quantities are known except M.

2. Taking $\beta = 6000$, then M the flux per pole is equal to $\beta \times$ total polar area.

The total polar area $= \tau\lambda g$.

Hence $M = 6000\,\tau\lambda g$ . . . . (2)

The turns per phase will in this case be given by $T = \dfrac{V}{MkN} \times 10^{-8}$.

By either of the above methods we arrive at preliminary values for the flux and armature turns.

As the output coefficient $\xi = 0.182 \times 10^{-8}\,\alpha\beta$ we must in any particular case choose the values for α and β in accordance with the particular value of ξ which has been taken. If ξ has a low value, α and β and the coefficients in equations (1) and (2) will be assigned lower values than those above set forth.

If the machine is required to have close pressure regulation, it must have a relatively "weak" armature which corresponds to a smaller value for α. Machines required to run very cool, and to stand large overloads, will have smaller values for β in order to provide a larger surface for the armature.

It does not follow that either value thus obtained for the flux and turns; will be adhered to finally. On the contrary, the values are, as the design progresses, adjusted to give the best results.

DESIGN OF 650 KVA. ALTERNATOR.

Without generalising further, let us apply this procedure to the preparation of a design for a 650 kva. 3-phase 4-pole 1500 R.P.M. 50-cycle 500-volt alternator.

The rating of an alternator may be stated either in kva. or in kw. at a certain power factor.

If, for instance, the machine under consideration were rated at 650 kw. at a power factor of 0.80, the output in kva. is $\dfrac{650}{0.80} = 812$ kva.,

and it should be designed for this number of kilovolt-amperes. The machine is Y-connected, and the volts per phase $= \frac{500}{\sqrt{3}} = 289$. The current per phase $= \frac{650,000}{289 \times 3} = 75$ amperes, which is also the current per terminal.

Number of poles $P = \frac{120 \times N}{R} = \frac{120 \times 50}{1500} = 4.$

The number of poles is at once fixed by the speed and frequency. Table 18 gives the relation between speed, frequency, and number of poles.

Value for $D^2\lambda g$. — From the curves in Chapter II a suitable value for ξ is found to be 1.23. Hence $D^2\lambda g = \frac{VI}{\xi \times R} = \frac{650,000}{1.23 \times 1500} = 352$ cubic decimetres.

From the curves in Fig. 70 the value of 75 centimetres is chosen for D. Before deciding to proceed with the design with this value for D, it is useful to take one or two values below and above this and tabulate for inspection the values for λg, the peripheral speed, s, $\lambda g/\tau$ and $\frac{D}{\lambda g}$ as is shown in Table 17:

TABLE 17.

DETERMINATION OF PRINCIPAL DIMENSIONS OF ALTERNATOR.

	D.	D^2.	λg.	s.	τ.	$\frac{\lambda g}{\tau}$.	$\frac{D}{\lambda g}$.
I	70	4900	72	55	55	1.31	0.97
II	75	5620	62.5	59	59	1.06	1.20
III	80	6400	55	62.8	62.8	0.87	1.45
IV	85	7220	49	67	66.6	0.73	1.73

The peripheral speed s in metres per second $= \frac{\pi D}{100} \times \frac{R}{60}$. In Table 19 we have tabulated values for s corresponding to various values of D and R. This table will be found useful for reading off directly the peripheral speed.

For the design in hand, Alternative III will probably lead to the best design, as the peripheral speed is not excessive, and the value of $\frac{\lambda g}{\tau}$ is lower. The latter ratio is worth keeping in mind, as it

TABLE 18.

SHOWING SPEEDS IN R.P.M. FOR DIFFERENT FREQUENCIES AND NUMBER OF POLES.

Frequency Cycles per Second.	Number of Poles.													
	2	4	6	8	10	12	16	20	24	28	32	40	48	64
20 . .	1200	600	400	300	240	200	150	120	100	86	75	60	50	37
25 . .	1500	750	500	375	300	250	187.5	150	125	107	94	75	62	47
30 . .	1800	900	600	450	360	300	225	180	150	128	112	90	75	56
35 . .	2100	1050	700	525	420	350	262.5	210	175	150	131	105	87	66
40 . .	2400	1200	800	600	480	400	300	240	200	171	150	120	100	75
50 . .	3000	1500	1000	750	600	500	375	300	250	214	187	150	125	94
60 . .	3600	1800	1200	900	720	600	450	360	300	257	225	180	150	112
80 . .	4800	2400	1600	1200	960	800	600	480	400	343	300	240	200	150
100 . .	6000	3000	2000	1500	1200	1000	750	600	500	428	375	300	250	187

TABLE 19.

SHOWING PERIPHERAL SPEEDS IN METRES PER SECOND FOR DIFFERENT DIAMETERS AND ANGULAR SPEEDS.

Speed in R.P.M.	Diameters in Centimetres.													
	20	40	50	60	80	100	150	200	250	300	350	400	450	500
100 . .	1.05	2.09	2.62	3.14	4.19	5.24	7.85	10.5	13.1	15.7	18.35	20.9	23.4	26.2
200 . .	2.09	4.19	5.24	6.28	8.38	10.5	15.7	20.9	26.2	31.4	36.7	41.9	47.0	52.3
300 . .	3.14	6.28	7.85	9.42	12.5	15.7	23.6	31.4	39.2	47.2	55.0	62.9	70.5	78.5
400 . .	4.19	8.38	10.5	12.5	16.8	20.9	31.4	41.9	52.3	62.9	73.4	84.0	94.0	104.6
500 . .	5.24	10.5	13.1	15.7	20.9	26.2	39.3	52.3	65.4	78.5	91.7	104.6	118.0	131.0
750 . .	7.85	15.7	19.6	23.6	31.4	39.3	59.0	78.5	98.2	118.0	137.5	...	...	...
1000 . .	10.5	20.9	26.2	31.4	41.9	52.4	78.5	105.0	131.0	...	...	...	...	...
1250 . .	13.1	26.2	32.8	39.3	52.4	65.5	98.3	131.0	...	...	...	...	...	...
1500 . .	15.7	31.4	39.3	47.1	62.8	78.5	118.0	...	...	...	...	...	...	...
2000 . .	20.9	41.9	52.3	62.7	83.6	104.6	...	...	...	...	...	...	...	...
2500 . .	26.2	52.4	65.5	78.5	104.6	...	...	...	...	...	...	...	...	...
3000 . .	31.4	62.8	78.5	94.2	...	...	...	...	...	...	...	...	...	...

practically determines the proportions of the section of the magnet cores, on which depends the weight of the field copper on the pole. The weight of copper for a given number of ampere turns and a given magnetic cross section of the pole, is a minimum when the pole is of circular section, and if of rectangular section the weight is smaller the nearer the shape of the section approximates to a square.

c, the breadth of the pole core (normal to the shaft), is usually from $0.35\,\tau$ to $0.50\,\tau$ (as the pole arc b averages $0.7\,\tau$ and the breadth of the pole core 0.5 to 0.7 of the pole arc).

The dimensions of the pole core section are $c \times \lambda g$, and if $c = 0.50\,\tau$ we have $\frac{\lambda g}{\tau} = 0.35$ to $0.50\frac{\lambda g}{c}$.

If the magnet core is of square section, then

$$c = \lambda g \quad \text{and} \quad \frac{\lambda g}{\tau} = 0.35 \text{ to } 0.50.$$

Thus, the smaller the ratio of $\frac{\lambda g}{\tau}$ (down to a value of about 0.35), the better is the economy of the field copper. On this point the 85-centimetre diameter design would be still better, but the peripheral speed is rather higher than is desirable in this case. (It is of interest to examine any of the continuous current designs given in Part III of this book. Most of these designs have pole cores of circular section, and $\frac{\lambda g}{\tau}$ ranges from 0.4 to 0.8.)

Determination of the Flux. — Having decided tentatively on Alternative III, we have $D = 80$, $\lambda g = 55$, $\tau = 62.8$.

Taking the pole arc as 60 per cent of the pole pitch, we have

$$b = 0.6 \times 62.8 = 38 \text{ centimetres}.$$

As we have not a very high output coefficient, we will take the air gap density, βg, at 7000 lines per square centimetre.

The pole face area is

$$\lambda_g b = 55 \times 38 = 2100 \text{ square centimetres},$$

and the flux per pole $M = 2100 \times 7000 = 14.7$ megalines, which we may take as a preliminary value.

Number of Armature Conductors. — The number of turns per phase is given by the equation.

$$T = \frac{V}{MkN} \times 10^8.$$

V = voltage per phase = 289.
M = flux per pole = 14.7×10^6.
N = frequency = 50.
k = coefficient obtained from Fig. 71.

We have $\frac{b}{\tau} = 0.6$ and the "spread" of the winding = 33 per cent. Whence $k = 4.7$. Then,

$$T = \frac{289 \times 10^8}{14.7 \times 10^6 \times 4.7 \times 50} = 8.4.$$

We must take the nearest whole number, which will be 8 or 9. The actual number of turns is now determined by the number of slots and the number of conductors per slot.

Number of Slots. — This is fixed by the choice of a number of slots per pole per phase and by considerations of the slot pitch. Large slow speed flywheel alternators with many poles and small pole pitch, have from one to three slots per pole per phase. In high speed alternators the number of slots per pole per phase ranges from 4 to 9 by reason of the few poles and the large pole pitches employed. For the case in hand we may take 4 or 5; but it is the best plan before deciding, to make out a table for a few different values as follows:

TABLE 20.

DETERMINATION OF NUMBER OF SLOTS AND TURNS IN ALTERNATOR DESIGN.

	No. of Slots per Pole per Phase.	No. of Slots per Phase.	No. of Slots per Pole.	Total No. of Slots.	Slot Pitch Cms.	No. of Conductors per Slot.	No. of Turns per Phase.	Ampere Cond. per Cm. of Periphery a.
I	4	16	12	48	5.23	1	8	144
II	5	20	15	60	4.18	1	10	180
III	6	24	18	72	3.49	1	12	216

We may take either Alternative I, II, or III; and of these, II will probably be the best.

In No. I with 8 turns per phase, the value for α, the ampere conductors per centimetre, is rather lower than should preferably be

employed on a machine of this rating. In No. III, α is somewhat high, and with 12 turns per phase the flux is considerably smaller, and the dimensions λg and τ might be reduced on account of the smaller

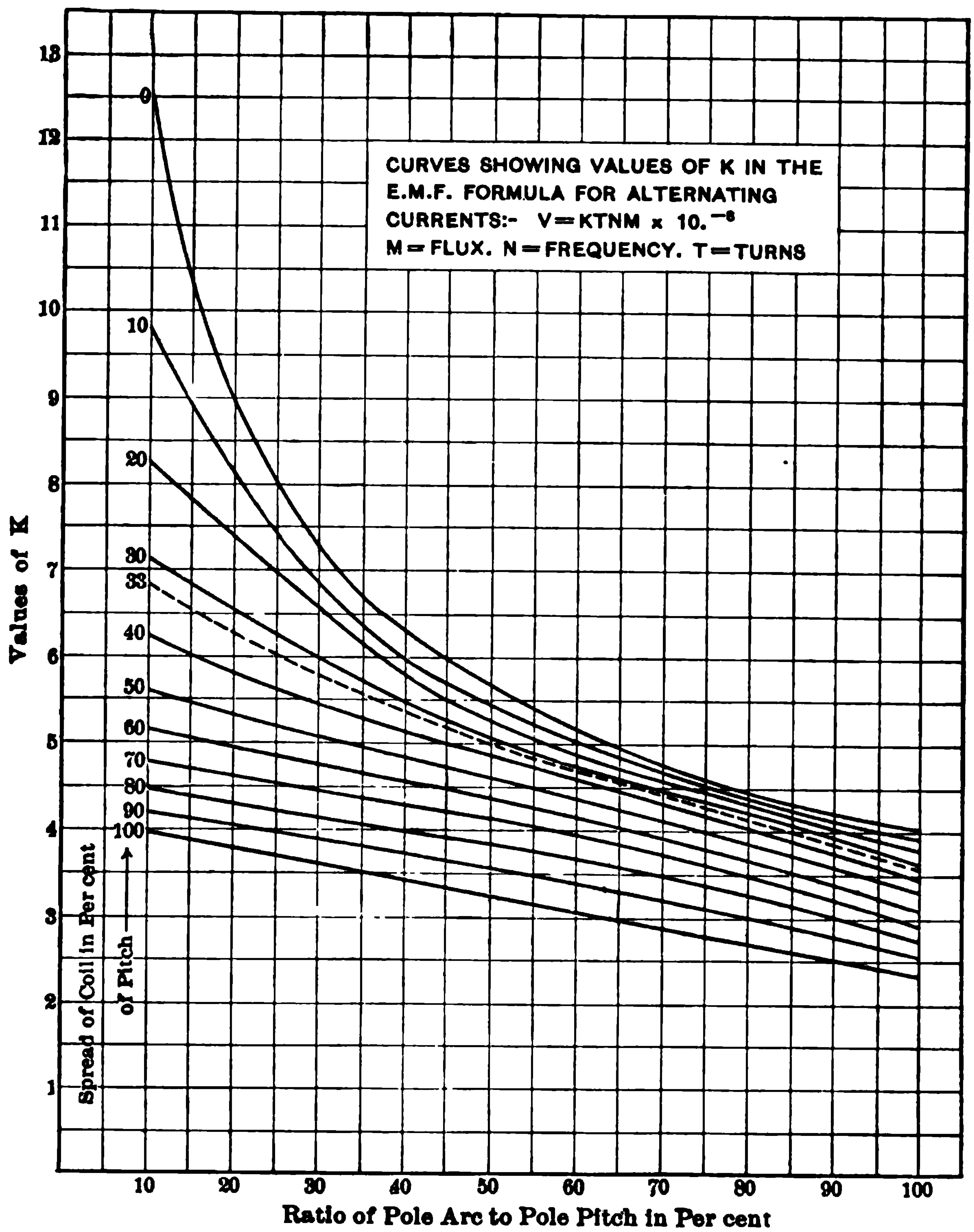


FIG. 71.— Curves showing values of K, the "voltage coefficient," in the E.M.F. formula.

flux, but this would reduce the radiating surface of the armature, and render the armature heating too high, as we shall see.

In a low voltage design, we have not much choice as to varying the number of turns per phase. For example, in the design considered, we must have either 8, 10, or 12 turns per phase (unless we have

recourse to an abnormal winding for the armature). If this design were for 5000 terminal volts instead of 500, we should have 80, 100, or 120 turns per phase with 10 conductors per slot. We could in this case vary the number of turns to a finer degree by taking 8 or 9, 11, or 12 conductors per slot. The question of whether we employ large or small slots, together with a large or small slot pitch, is of importance. The greater the number of slots per pole per phase and the smaller the slot, the lower is the space factor that can be obtained in the slot for a given voltage. In the present case and in any low voltage design, this point is not of serious importance, as the slot insulation will not occupy much room. Had this design been for 5000 volts, we should have preferred the 48-slot design on this account, but with 500 volts we shall take 60 slots with one conductor per slot. We are now able to recalculate the final value of the flux with 10 turns per phase:

$$M = \frac{V}{KTN} \times 10^8 = \frac{289}{4.7 \times 10 \times 50} \times 10^8 = 12.3 \text{ megalines per pole.}$$

This is the flux crossing the air gap and entering the armature: The flux in the poles is obtained by multiplying the armature flux by the leakage coefficient (see page 76). We have now to assign values to the magnetic densities at the different parts of the magnetic circuit, to determine the sizes of the armature body, teeth and pole cores.

The following values should not generally be exceeded, and it is preferable in alternator design, to work as near them as practicable:

TABLE 21.

MAXIMUM FLUX DENSITIES IN IRON AND STEEL.

Pole cores of wrought iron or forged steel .	16,000 lines per sq. cm.
Pole cores of cast steel	15,000 lines per sq. cm.
Pole cores of cast iron	9,500 lines per sq. cm.
Teeth, best sheet iron or steel stampings . .	20,000 lines per sq. cm.
Armature body (below slots)	10,000 lines per sq. cm.

Magnet Poles. — The density will depend on the material employed. Rotating fields for turbine speeds are either of laminated sheet iron or steel, or of some variety of iron or steel forgings, and, in a few cases, steel castings.

For the machine considered, the rotor is built from slabs of forged steel (see Fig. 72), and we may take a density of 16,000 lines per square centimetre.

To obtain the flux in the pole, we require a value for the leakage coefficient of the magnetic circuit. This can be calculated in the manner shown on page 75, Chapter V, if the dimensions of the poles are known.

For the present purpose we shall assume a preliminary value, for which a fair figure in this case will be 1.15. The flux in the pole is $1.15 \times 12.3 = 14$ megalines. Taking a density of 16,000, the magnetic section required is $\frac{14 \times 10^6}{16{,}000} = 875$ square centimetres. The length of the pole parallel to the shaft is λg minus the length occupied by the air ducts in the field system. The rotor is built of 4 slabs of forged steel with 3 ducts between the slabs, the centre one

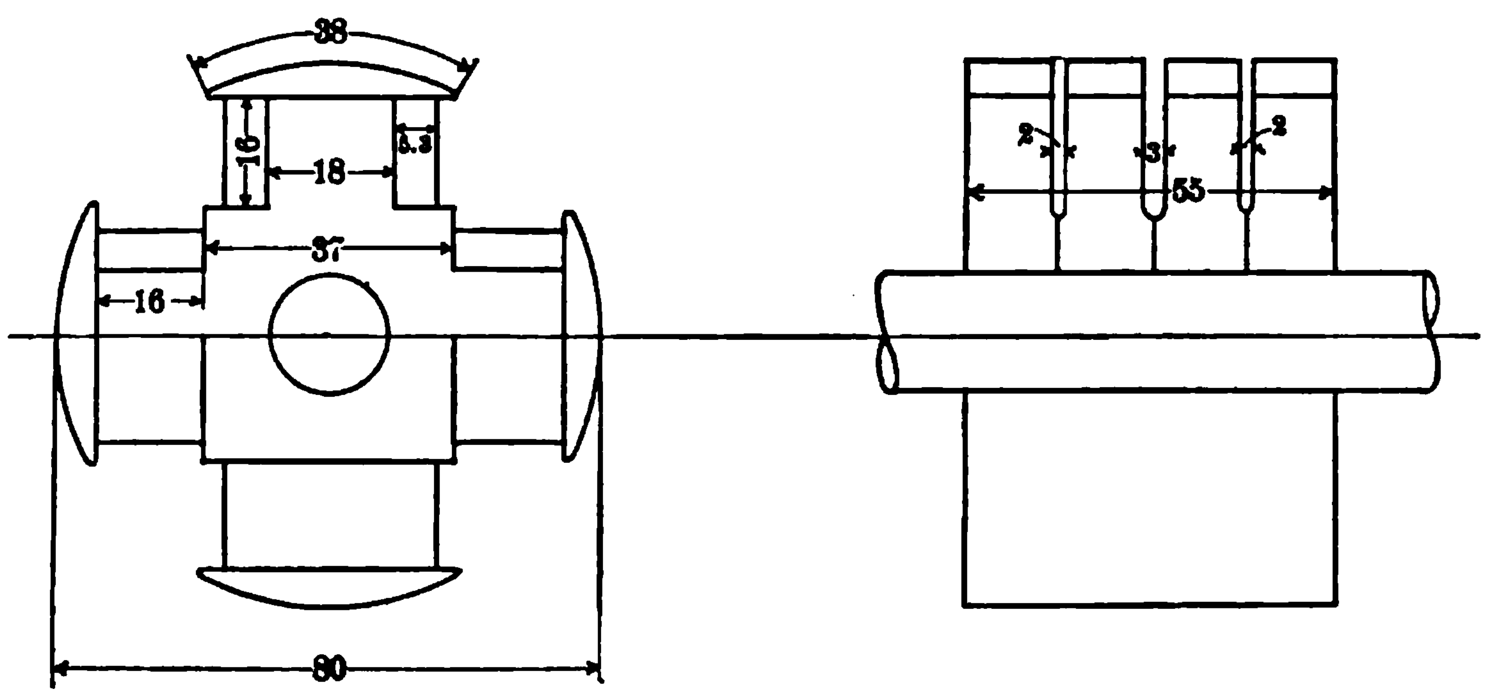


Fig. 72.— Outline of field of 650 kva. alternator.

being 3 centimetres wide, and the other two 2 centimetres wide. The net length is thus $55 - (2 + 2 + 3) = 48$ centimetres. The breadth of the pole will be $\frac{880}{48} = 18$ centimetres. An outline sketch of the form shown in Fig. 72 can now be made. It is desirable to use this outline to ascertain whether there will be room for the requisite amount of field copper on the poles. It is a somewhat difficult problem, in high speed alternator design, to find sufficient space for the field copper without undue heating and stresses; hence it is a good plan to look roughly into this point at this early stage in the design, since if left to a later stage, when the final calculations for the field winding are made, the whole design may have to be considerably modified, thus entailing a large amount of wasted work. Some idea is needed of the total ampere turns which will be required on each pole, and this can be obtained from the armature strength, since the number of turns have been settled.

We have $750 \times 10 = 7500$ ampere turns per phase, or $3 \times 7500 = 22{,}500$ ampere turns on the whole armature. The armature ampere turns per pole or **armature strength** $= \frac{22500}{4} =$ **5625.** The field ampere turns may be estimated as a preliminary step, by assuming a value for the short circuit current. With modern iron clad alternators, the short circuit current for normal field excitation and speed is from two to four times the full load armature current for machines of normal regulating properties.

The armature demagnetisation on short circuit is about $0.7 \times$ the armature strength, the power factor being practically zero. If we assume a short circuit current equal to 2.5 times the full load current, the armature demagnetisation on short circuit is $2.5 \times 0.7 \times$ the armature strength, or nearly twice the armature strength. This reaction counterbalances the field excitation ampere turns for normal voltage at no load, and hence we have a preliminary value for the latter. In our case the demagnetising ampere turns on short circuit will be $2.5 \times 0.7 \times 5625 = 9800$ ampere turns. The ampere turns on each pole at no load will be about 9800, but the maximum excitation will occur when the load has its lowest power factor which may be taken as 0.8. To maintain normal voltage for such a load the excitation regulation is about 40 per cent, and the field ampere turns are $1.4 \times 9800 = 13{,}700$.

The question now is whether the field system, as already outlined, will provide sufficient space for 13,700 ampere turns per pole. The current density usually ranges from 200 to 400 amperes per square centimetre. Taking the lower value, the total cross section of copper is $\frac{13700}{200} = 68$ square centimetres.

With strip wound coils, we can obtain a space factor of 0.8 so that the total cross section of winding space required is $\frac{68}{0.8} = 85$ square centimetres. The available winding length from pole seat to underside of shoe is 16 centimetres, and the depth of the winding will be $\frac{85}{16} = 5.3$ centimetres. The outline of the winding section corresponding to these dimensions is marked out on the upper poles in Fig. 72, from which it will be seen that there is just room on the pole for this number of ampere turns.

If the available space is not large enough, it is necessary either to decrease the armature strength by employing less turns per phase, or to increase the diameter of the pole system to obtain a greater radial length under the pole seat. The former plan entails an in-

crease in the flux and in λg to carry the increased flux. The latter increases the peripheral speed. Both plans reduce the output coefficient.

Having ascertained that room can probably be found for the field copper, we may leave detailed calculations till a later stage and proceed further with the general design.

Slots and Teeth. — The width of the tooth is determined from the flux density in the teeth. With a flux of 12.3 megalines, the density at the pole face is only 6750 lines per square centimetre. As this is not high, we shall not be able to employ a very high density in the teeth if we wish to preserve a good proportion of slot width to tooth width.

In high speed machines, where the output coefficient and the air gap density are relatively low, the tooth densities are also relatively low. For our case we shall first take a tooth density $\beta t = 16{,}000$ lines per square centimetre. The magnetic section of the teeth to carry 12.3 megalines is,

$$\frac{12.3 \times 10^6}{16{,}000} = 770 \text{ square centimetres.}$$

The flux is carried by the number of teeth within the "effective" pole arc.

Allowing 10 per cent for fringing of the flux at the pole tips, the "effective" pole arc is $1.1 \times b$, where b = the circumferential length of the pole arc at the pole face. The number of teeth carrying the flux are then $\frac{1.1b}{\tau} \times Z = 1.1rZ$, where Z = the number of teeth per pole, and r = the ratio of pole arc to pole pitch.

If t = the width of tooth at armature face, the magnetic section is $1.1rZ\lambda_n t$. The armature net core length, λn, may be provisionally taken as $0.7\lambda g$, as such a ratio will usually allow of a fair supply of ventilating ducts.

(If we allow 10 per cent for core plate insulation, the length occupied by core plates and their insulation is $\frac{\lambda n}{0.9}$, and the space occupied by the ducts $\lambda g - \frac{\lambda n}{0.9}$. If $\frac{\lambda n}{\lambda g} = 0.7$, this becomes $\lambda g - \frac{0.7\lambda g}{0.9} = 0.22\lambda g$, i.e., the air ducts take up 22 per cent of the gross core length.)

In the design with which we are occupied, we have $r = 0.6$, $Z = 15$,

$\lambda n = 0.7 \times 55 = 38$, and the section of the teeth $= 1.1 \times 0.6 \times 15 \times 38t = 376t$. This must equal 770 square centimetres whence

$$t = \frac{770}{376} = 2.05 \text{ centimetres.}$$

The slot pitch is 4.19 centimetres and the slot width $s = 2.14$ centimetres. The ratio of slot width to tooth width is $\frac{s}{t} = 1.04$.

Depth of Slot. — This may be settled by taking a depth which in relation to the width, gives a well proportioned slot or by choosing a value for the current density in the armature conductors. The current density is ultimately determined by the permissible copper loss. In low speed machines the copper loss is, on the average, 0.4 to 0.7 of the iron loss; but in high speed machines with few poles and a great depth of iron below the slots, the copper loss is generally only 0.1 to 0.2 of the iron loss. In our present design we shall not be bound down to a low current density on account of the copper loss, but this may be expedient in order to keep the slot of good proportions.

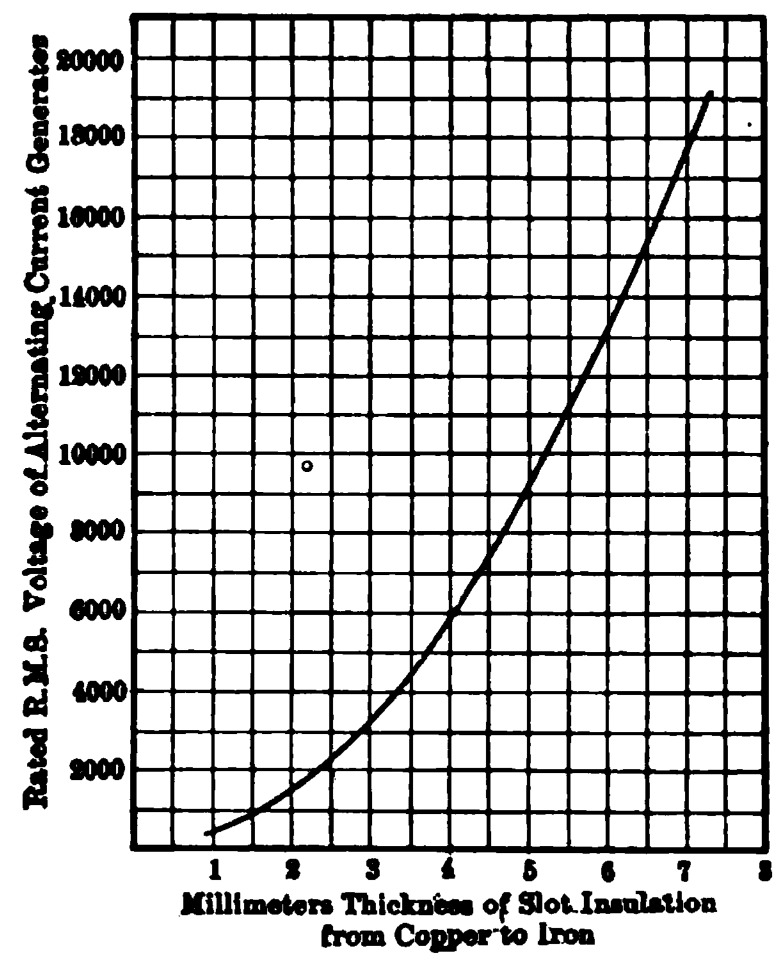


FIG. 73. — Curve showing thickness of armature slot insulation for alternating current generators.

If we assume a current density of 220 amperes per square centimetre, the cross section of the conductor for 750 amperes is $\frac{750}{220} = 3.4$ square centimetres.

Fig. 73 gives a curve for the thickness of slot insulation in terms of the voltage, and for 500 volts the slot lining will be 1 millimetre. This leaves $2.14 - 2 \times 0.1 = 1.94$ centimetres, and allowing 0.5 millimetre tolerance in assembling the laminations, we have 1.89 centimetres available for the conductors.

As the conductor is of large section, we shall take two in parallel, each consisting of 19 strands of number 10 B.W.G. pressed cable, insulated with 0.4 millimetre braid. The slot design is shown in Fig. 74.

Depth of Armature Stampings. — The armature flux density is finally settled with reference to the core loss, and it is necessary to take a preliminary value and estimate the core loss and the armature heating. An experienced designer is at once able to choose a value near the permissible one for the case in hand. With turbo-alternators the mean diameter of the armature body is large compared with the internal diameter, and the weight increases rapidly with the cross section for a given internal diameter. Hence for turbo-alternators, very low flux densities are not of much avail for reducing the core loss.

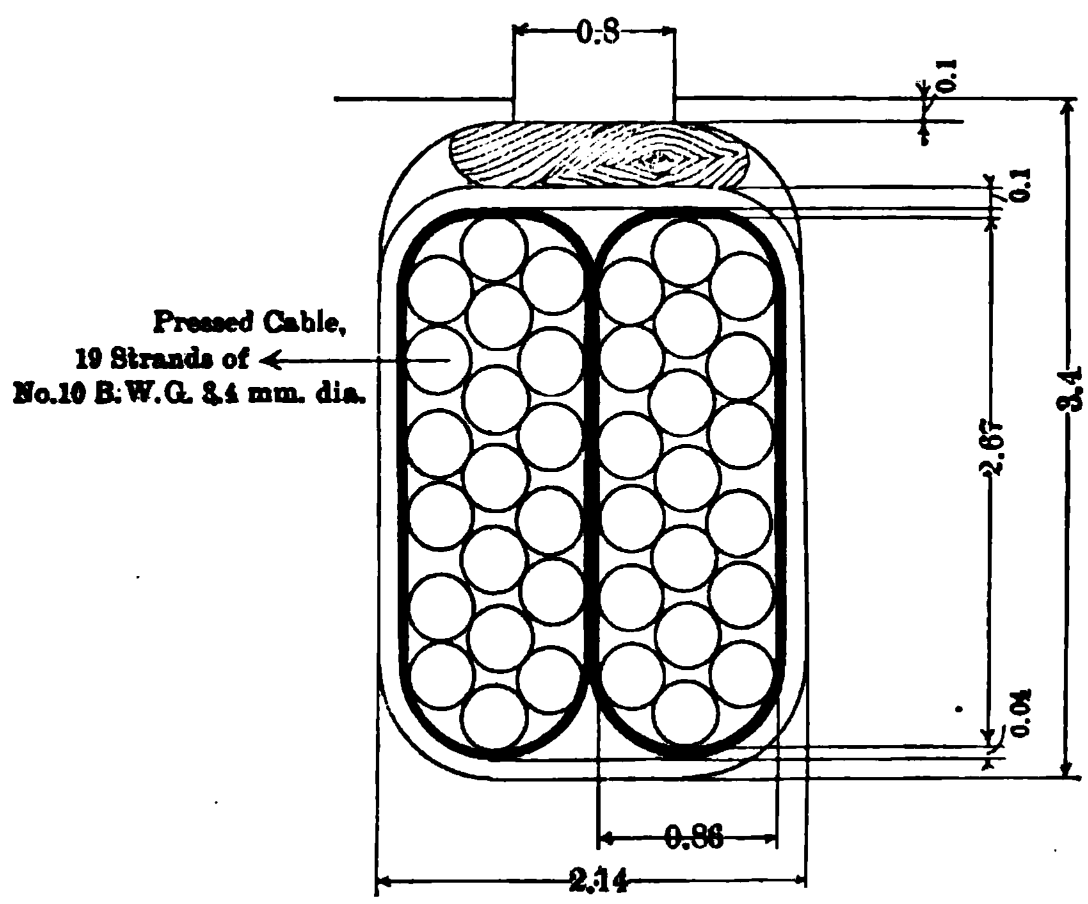


FIG. 74. — Armature slot for 650 kva. alternator.

In our case we will first take $\beta_a = 8000$ lines per square centimetre. The magnetic section is then $\frac{12.3 \times 10^6}{8000} = 1540$ square centimetres.

As the flux divides in the armature, the section on one side is $\frac{1540}{2} = 770$. The depth of stampings below the slots is,

$$\frac{770}{\lambda_n} = \frac{770}{38} = 20.$$

The external diameter is,

$$80 + 2\,(3.5 + 20) = 127 \text{ centimetres.}$$

A rough estimation of the armature losses and heating should now be made to ascertain if the machine comes within permissible limits as regards temperature rise.

Armature Iron Loss. — If the densities in the armature body and teeth are not near together in value, it is preferable to estimate separately the losses in the armature body and teeth. If the densities are more nearly equal, the weights of teeth and armature may be added together and then multiplied by one value for the watts per kilogram. Fig. 75 gives a curve for estimating the core loss.

Armature Body. — For our design we have $\beta_a = 8000$, N = 50, $\therefore$ the watts per kilogram = 7 (from Fig. 75).

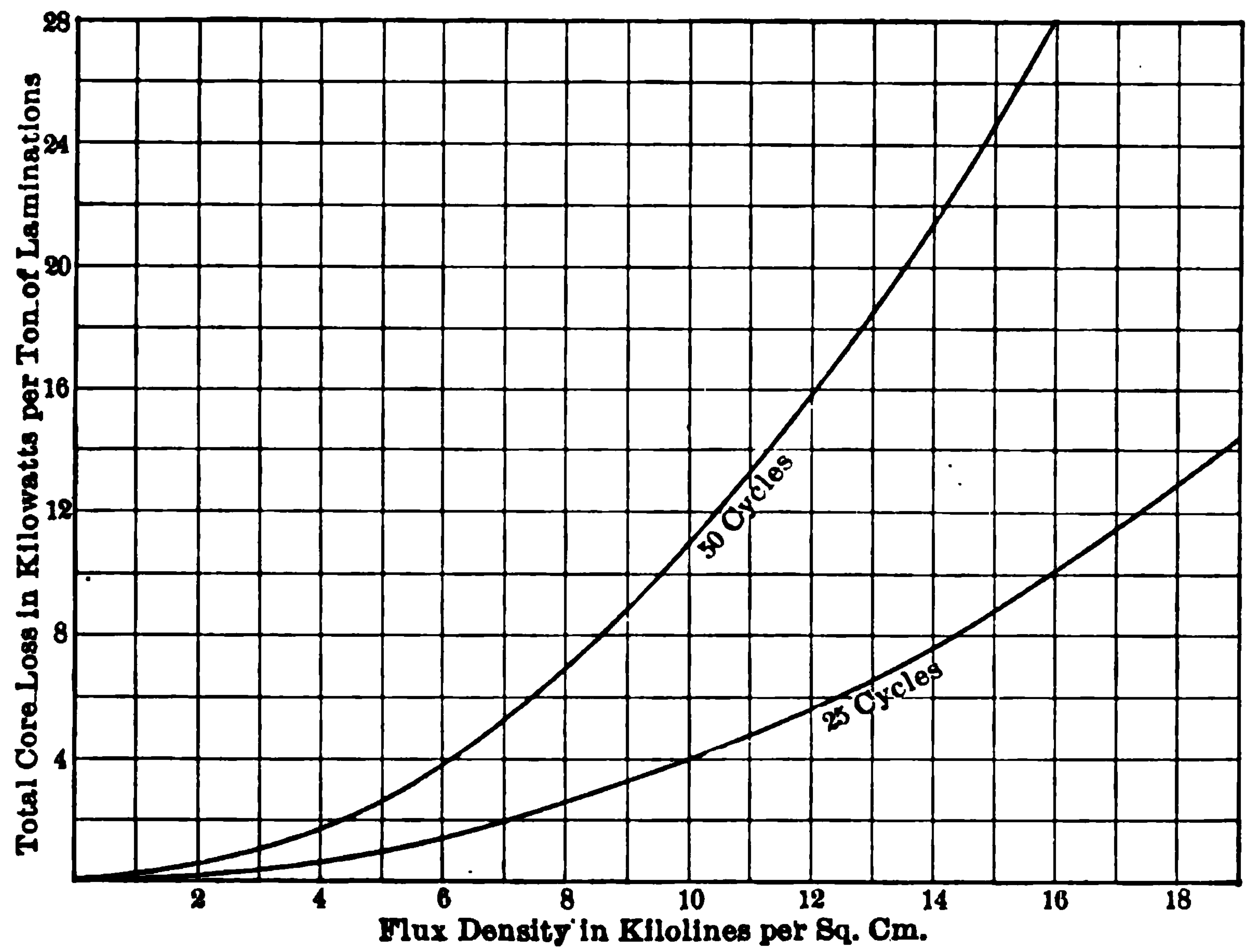


FIG. 75. — Curves for estimating the total armature core loss in alternating current generators.

The weight of the armature core is $\frac{\pi D_m \lambda_n h \times 7.8}{1000}$ kilograms where D_m is the mean diameter (halfway between the bottom of the slots and the external periphery) and h the radial depth of the stampings below the slots.

$$D_m = \frac{127 + 87}{2} = 107 \text{ centimetres}, \lambda_n = 38, h = 20.$$

$$\text{Weight} = \frac{\pi \times 107 \times 38 \times 20 \times 7.8}{1000} = 2000 \text{ kilograms.}$$

The core loss is 2000 $\times$ 7 = 14,000 watts. . . . . (1)

Teeth Loss. — $\beta_t = 15{,}500$ (mean density halfway up the tooth), $N = 50$, watts per kilogram $= 24$ (from Fig. 75).

$$\text{Weight of all teeth} = \frac{t_m \times d \times \lambda_n \times \text{number of teeth} \times 7.8}{1000},$$

t_m = mean width of tooth = 2.1.
d = depth of slot = 3.5.

$$\text{Weight} = \frac{2.1 \times 3.5 \times 38 \times 60 \times 7.8}{1000} = 130 \text{ kgs.}$$

$$\text{Loss} = 130 \times 24 = 3100 \text{ watts} \quad . \; . \; . \; . \; . \; . \; . \; . \; . \; . \; . \; . \quad (2)$$

Copper Loss. — The armature resistance per phase is required.

The mean length of a turn of the armature coil is approximately $= 2\lambda g + k\tau$, where k ranges from 3.5 to 5.5, the value of k being lower the lower the voltage and the less the number of poles. Taking for our case $k = 4$ we have

Mean Length Turn $= (2 \times 55) + (4 \times 62.8) = 360$ centimetres.

Since there are ten turns per phase, the length of conductor per phase $= 10 \times 360 = 3600$ centimetres.

The resistance per phase at 60 degrees C. $= \frac{l}{a}\rho = \frac{3600}{3.4} \times 0.000002$ $= 0.0021$ ohms.

The I^2R loss per phase is $750^2 \times 0.0021 = 1180$ watts and for 3 phases $= 3540$ watts . . . . (3)

Total Losses and Heating.

The total armature losses (1), (2), and (3) are thus $14{,}000 + 3100 + 3540 = 20{,}640$ watts. The armature radiating surface is

$$\begin{aligned} &\pi D(\lambda g + 0.7\tau) \\ &= \pi \times 80\,(55 + 0.7 \times 62.8) \\ &= 250 \text{ square decimetres.} \end{aligned}$$

The heating coefficient is thus $\frac{20640}{250} = 83$ watts per square decimetre, or, reckoned on the surface $\pi D\lambda g$, 150 watts per square decimetre.

If due attention is paid to the design of the ventilation channels, these values will give a rise of temperature within 40 degrees C. so that the design is within correct limits in this respect. Since the weight of armature iron decreases with increasing flux density, it is of interest at this point to calculate the losses heating and efficiency for several armature flux densities to ascertain the highest density that can be allowed.

Table 22 contains the necessary calculations for several densities ranging from 6000 to 10,000.

TABLE 22.

DETERMINATION OF ARMATURE FLUX DENSITY IN ALTERNATOR DESIGN.

β_a	Depth, h.	External Diameter.	Weight of Core.	Loss in Watts per Kg.	Core Loss.	Total Armature Losses.	Watts per Sq. Dm.		Efficiency Excluding Field and Friction Losses.
							$\pi D (\lambda g + 0.7\tau)$.	$\pi D\lambda g$.	
6,000	27	141	2860	4.0	11,500	18,140	73	131	97.3%
7,000	23	133	2350	5.4	12,700	19,340	77	140	97.2%
8,000	20	127	2000	7.0	14,000	20.640	83	150	97.0%
9,000	18	123	1760	9.0	15,800	22,440	90	163	96.7%
10,000	16	119	1540	11.0	17,000	23,640	95	171	96.5%

Calculation of Magnetic Circuit. — We have already settled the flux densities and cross sections of the "iron" parts, of the magnetic circuit, and before calculating the no load saturation curve there only remains to be settled the depth of the air gap.

Depth of Air Gap. — On page 115, taking the field ampere turns as 1.75 times the armature ampere turns, we found the required no load excitation per pole to be about 9800 ampere turns. Of this, some 80 per cent to 85 per cent will be taken up by the air gap as the iron is fairly highly saturated.

Taking the air gap ampere turns as amounting to 80 per cent we have

Air Gap Ampere Turns = 0.8 × 9800 = 7840.

The pole face density

$$\beta_g = \frac{M}{b \times \lambda_n} = \frac{12.3 \times 10^6}{48 \times 38} = \frac{12.3 \times 10^6}{1830} = 6750 \text{ lines per sq. cm.}$$

The depth of gap

$$l_g = \frac{\text{ampere turns}}{0.8\beta_g} = \frac{7840}{0.8 \times 6750} = 1.45 \text{ centimetres.}$$

In making magnetic calculations of alternators, it is convenient to calculate the excitation for normal voltage and for 1.2 times normal voltage, as these will give two points on the saturation curve. As the voltage regulation should not exceed 20 per cent, no part of the saturation curve beyond a point corresponding to a voltage 20 per cent above the normal voltage, will be required.

The necessary calculations are set forth in Table 23.

TABLE 23.

CALCULATIONS OF FIELD EXCITATION AT NO LOAD IN DESIGN OF 650 KVA. ALTERNATOR.

	Normal Voltage per Phase, 289 Volts.	1.2 × Normal Voltage, 347 Volts.
ARMATURE		
Flux per pole	12.3	14.8
Magnetic area (A_a)	1,540	1,540
Flux density (β_a)	8,000	9,600
Magnetic length (l_a)	42	42
Ampere turns per cm.	3	4
Ampere turns	126	168
TEETH		
Flux	12.3	14.8
Magnetic area (Teeth under pole face) + 10% (A_t)	770	770
Flux density (β_t)	16,000	19,200
Magnetic length (l_t)	3.5	3.5
Ampere turns per cm.	25	75
Ampere turns	88	263
AIR GAP		
Flux	12.3	14.8
Area at pole face (A_g)	2,100	2,100
Flux density (β_g)	5,850	7,050
Magnetic length (l_g)	1.45	1.45
Ampere turns per cm. $= 0.8 \times \beta_g$	4,670	5,640
Ampere turns	6,770	8,200
POLE CORE		
Leakage coefficient	1.15	1.15
Flux in the pole	14	16.8
Magnetic area (A_p)	875	875
Flux density (β_p)	16,000	19,200
Magnetic length (l_p)	20	20
Ampere turns per cm.	25	75
Ampere turns	500	1,500
YOKE OR HUB		
Flux	14	16.8
Magnetic area (A_y)	930	930
Flux density (β_y)	15,000	18,000
Magnetic length (l_y)	10	10
Ampere turns per cm.	17	60
Ampere turns (A.T.$_y$)	170	600
SUMMARY		
Ampere turns for yoke	170	600
Pole	500	2,000
Pole + yoke	670	2,600
Teeth	88	455
Armature	126	168
Total for iron	884	3,223
Air gap	6,770	8,200
Total for no load	7,654	11,423
Air gap ampere turns in per cent of total	88.5%	71.5%

The no load saturation curve is drawn in Fig. 76.

Armature Reactions and Regulation. — The estimation of the armature reactions and the pressure regulation may now be carried out in detail. This is done in the manner already outlined in Chapter V, and below are given the necessary calculations set out in order. The items as arranged, form a convenient basis of a schedule for calculating regulation by this method, and will also serve as a tabular illustration of the method.

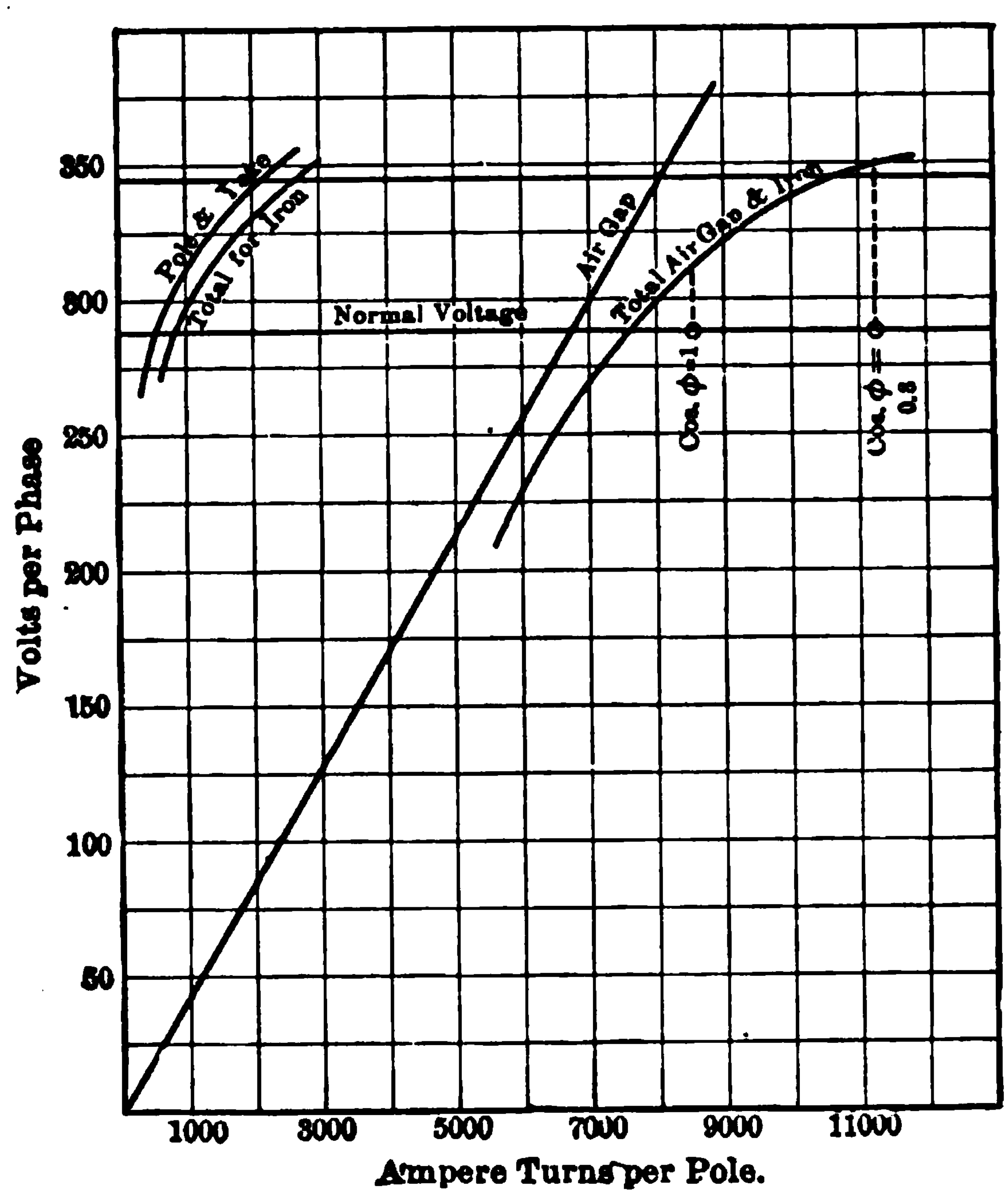


FIG. 76.— Saturation curve for 650 kva. 3-phase 4-pole 1500 r.p.m. 50-cycle 500-volt alternator.

TABLE 24.

CALCULATION OF ARMATURE REACTIONS AND REGULATION IN DESIGN FOR 650 KVA. ALTERNATOR.

ARMATURE INDUCTANCE.

Mean length of turn $= 2\lambda g + 4\tau$	360
Embedded length $= 2\lambda n$	76
Free length per turn (L.M.T $- 2\lambda n$)	284
C.G.S. lines per A.T. of embedded length (76×0.4)	30
C.G.S. lines per A.T. of free length (284×0.5)	142
C.G.S. lines per ampere turn	172

TABLE 24 — *Continued.*

CALCULATION OF ARMATURE REACTIONS AND REGULATION IN DESIGN FOR 650 KVA. ALTERNATOR.

Number of turns per coil		5
Inductance of one coil $= (5^2 \times 158 \times 10^{-8})$		0.000043
Reactance of one coil (ohms)		0.0135
Coils per phase		2
Reactance per phase (ohms)		0.0270
Reactance pressure per phase at full load current	volts	21.0
Reactance pressure	per cent	7.0
Number of turns per phase		10
Armature ampere turns per phase at full load		7,500
Armature ampere turns for all phases		22,500
Armature ampere turns per pole	ni	5,625
Effective demagnetising amp. turns $KBni = 0.75 \times 0.95 \times 5625$		4,000
Field ampere turns for reactance voltage (from no load saturation curve)		350
Total field ampere turns for full load current on short circuit		4,350
No load field ampere turns (full terminal volts)		7,654
Ratio of short circuit current to full load current		1.76
Short circuit current	amperes	1,320

REACTION CALCULATIONS.

I. Power Factor = 1.0

$\cos \phi$		1.0
ϕ		0
(α) Reactance voltage	per cent	7.0
$\sin \alpha = \sin\left(\tan^{-1}\dfrac{Rv}{Tv}\right)$		0.07
α in degrees		4.0
(β) distorting ampere turns = $KBni \cos \phi' = 0.15 \times 0.95 \times 5625 \times 0.98$		782
$\beta = \dfrac{\text{Distorting ampere turns} \times 90^\circ}{\text{Ampere turns for air gap}}$		10.0
(ϕ') $\phi' = \phi + \alpha + \beta$ in degrees		14
$\sin \phi'$		0.242
(A) Demagnetising ampere turns (effective) $= KBni \sin \phi' = 0.75 \times 0.95 \times 5625 \times 0.242$		970
No load leakage coefficient ($= 1 + \sigma$)		1.15
Increased leakage coefficient $= 1 + \left(\sigma \dfrac{\text{Full load amp. turns}}{\text{No load amp. turns}}\right)$		1.17
Equivalent increased voltage $\dfrac{1.17}{1.15} \times 289$		294
(B) Extra ampere turns (from Fig. 76 pole and yoke curve)		130
Total extra field ampere turns at full load = A + B		1,000
No load ampere turns		7,650
Total ampere turns for full load $\cos \phi = 1.0$		8,650

TABLE 24 — *Continued.*

CALCULATION OF ARMATURE REACTIONS AND REGULATION IN DESIGN FOR 650 KVA. ALTERNATOR.

II. *Power Factor* = 0.8		
$\cos\phi$		0.8
ϕ in degrees		37
(α) Reactance voltage	per cent	7.0
$\sin\alpha = v\cos\phi$		0.056
α in degrees		3.1
(β) Distorting ampere turns =		
$KBni \cos\phi' = .15 \times .95 \times 5625 \times 0.669$		536
$\beta = \dfrac{\text{Distorting ampere turns} \times 90°}{\text{Ampere turns for air gap}}$		7.1
(ϕ') $\phi' = \phi + \alpha + \beta$		47°
A. Demagnetising ampere turns (effective)		
$KBni \sin\phi' = 0{,}75 \times 0.95 \times 5625 \times 0.731$		2,930
Reactance voltage (volts)		18.6
$v \sin\phi$		11.2
B. Ampere turns for reactance drop		300
No load leakage coefficient $= 1 + \sigma$		1.15
Increased leakage coefficient $= 1 + \left(\sigma \dfrac{\text{Full load A.T.}}{\text{No load A.T.}}\right)$		1.21
Equivalent increase in voltage		10
C. Field ampere turns		250
Total extra field ampere turns at full load $= A + B + C$		3,480
No load ampere turns		7,650
Total ampere turns for full load ($\cos\phi = 0.8$)		11,130

REGULATION.

I. $\cos\phi = 0.8$		
Field ampere turns for full load		11,130
Corresponding voltage at no load		350
Inherent regulation	volts	61
Inherent regulation	per cent	21%
Resistance drop	per cent	1%
Total regulation		22%
Excitation regulation		40%
II. $\cos\phi = 1.0$		
Field ampere turns for full load		8,650
Corresponding voltage at no load		310
Inherent regulation	volts	19
Inherent regulation	per cent	6.5%
Resistance drop	per cent	1.0%
Total regulation		7.5%

The design may now be regarded as complete so far as the electromagnetic design is concerned. As in high speed alternators the mechanical problems are prominent and are interlinked with the electrical and magnetic problems, it is necessary to make calculations of the stresses in the various parts of the rotor, in order to ascertain

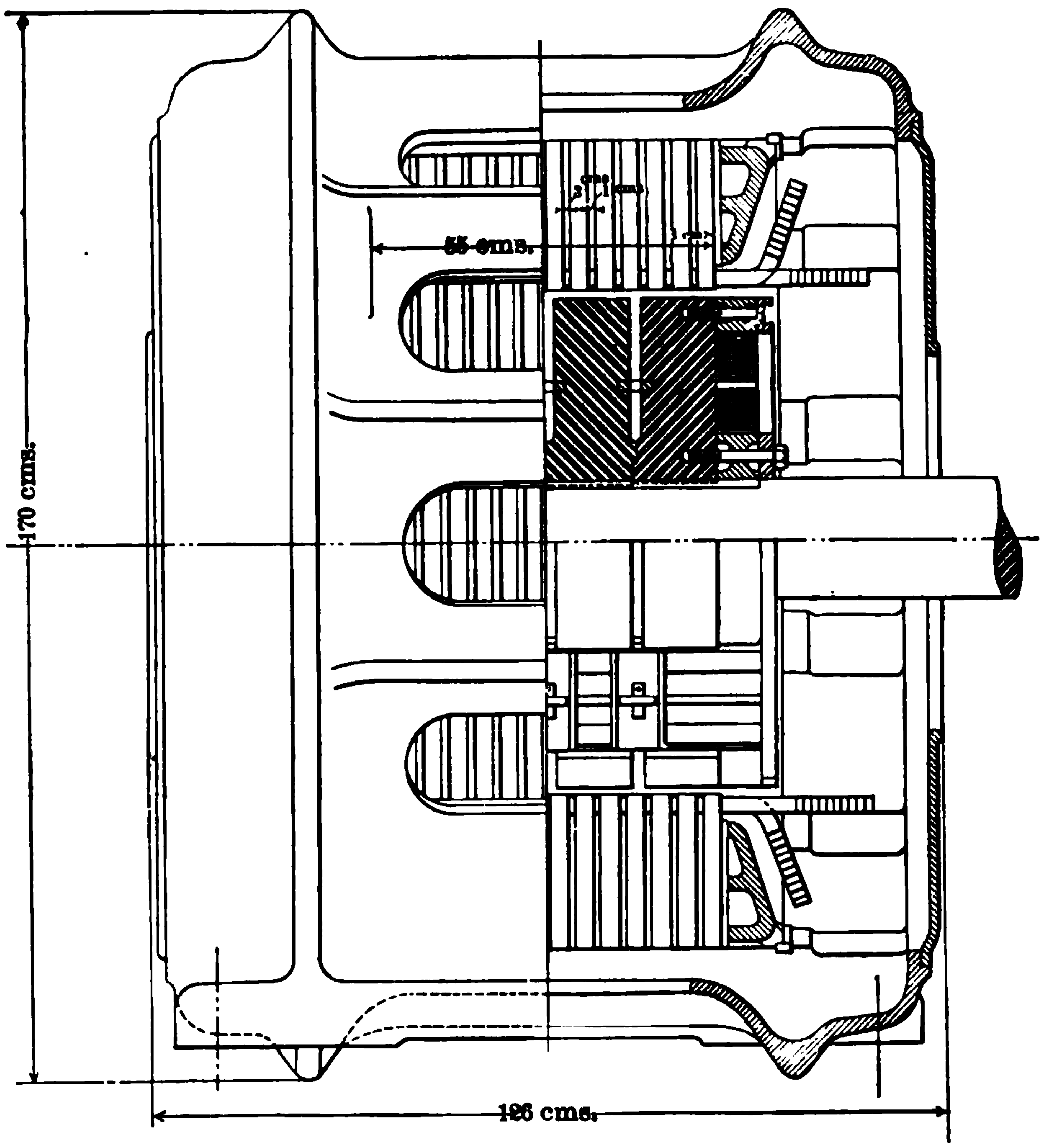


FIG. 77.— Outline drawing of assembly of 650 kva. 1500 r.p.m. 50-cycle 4-pole 3-phase alternating current generator.

whether the proportions required by the electrical and magnetic conditions are sufficient for the mechanical conditions.

Such calculations need not go so far as determining in detail the dimensions of the mechanical accessories, but they should form an integral part of the designer's problem. The method of making such outline calculations is set forth in Chapter XII, in which an alternative structure for the 650 kva. machine, which we have designed in this chapter, is handled in considerable detail.

For this 650 kva. machine the design is summarised in the following specification. Drawings indicating the mechanical design are given in Figs. 77 and 77 A.

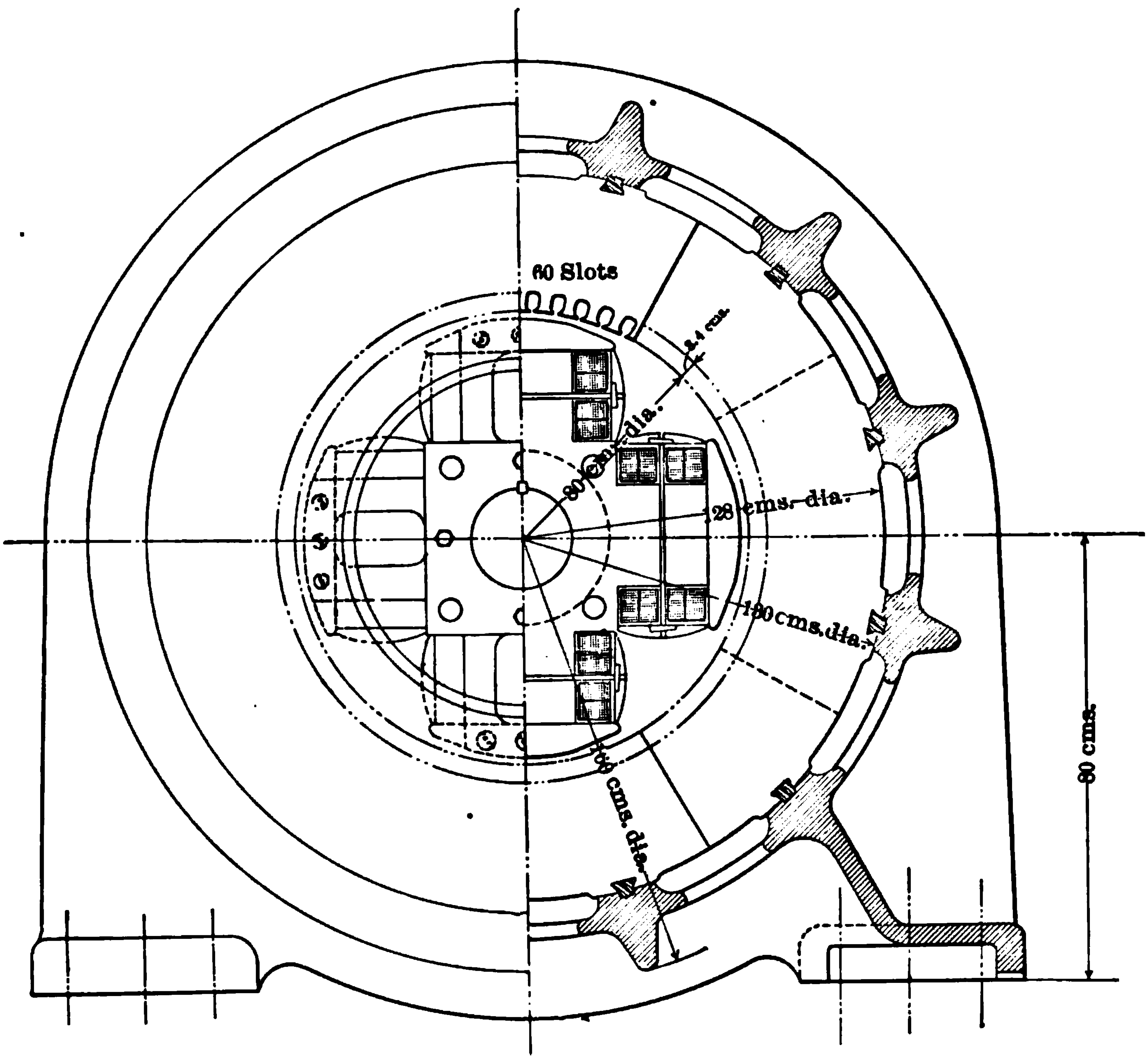


FIG. 77 A. — Outline drawing of assembly of 650 kva. 1500 r.p.m. 50-cycle 4-pole 3-phase alternating current generator.

SPECIFICATION FOR 650 KVA. 3-PHASE ALTERNATOR.
4 poles, 500 volts, 1500 R.P.M., 50 cycles.

Output kilovolt amperes	650
Terminal voltage	500
Style of connection	*Y*
Current per terminal	750
Speed R.P.M.	1500
Frequency	50
Number of poles	4
Type	Internal revolving field

SPECIFICATION FOR 650 KVA. 3-PHASE ALTERNATOR — *Continued.*

ARMATURE IRON	*Centimetres.*
Diameter at air gap	80
Diameter at bottom of slots	86.8
External diameter of laminations	127
Gross length between core heads	55
Number of ventilating ducts	13
Width of each duct	1
Percentage insulation between stampings	10%
Effective core length (iron)	37
Width of the end ducts	1

SLOTS AND TEETH	
Total number of slots	60
Type of slot	semi closed
Slot pitch at armature face	4.19
Width of slot	2.14
Width of slot opening	0.8
Width of tooth at armature face	3.39
Thickness of tip of tooth	0.1
Width of tooth at narrowest part	2.05
Radial depth of slot	3.4

ARMATURE COPPER	
Number of slots per pole per phase	5
Number of conductors per slot	2
Number in parallel per slot	2
Each conductor consists of 19 strands of No. 10 B.W.G. pressed cable.	
Section of 19/10 B.W.G. =	1.73 sq. cms.
Section of two in parallel	3.46 sq. cms.
Current	750 amperes
Current density — amperes per sq. cm.	217
Outside dimensions of conductor, bare	3.0 × 0.87
Outside dimensions of conductor, covered	3 08 × 0.95
Nature of covering	braiding
Thickness of slot insulation	1 mm.
Number of turns in series per phase	10
Number of coils per phase	2
Number of turns per coil	5

ROTOR IRON	
Diameter over poles	77.5
Length of air gap (minimum)	1.25

SPECIFICATION FOR 650 KVA. 3-PHASE ALTERNATOR — *Continued.*

ROTOR IRON —*Continued.*	*Centimetres.*
Pole pitch at air gap	63
Circumferential length of pole arc	38
Ratio $\frac{\text{pole arc}}{\text{pole pitch}}$	60%
Gross axial length (parallel to shaft)	55
Number of ventilating ducts	3
Width of middle duct	3
Width of other ducts	2
Effective axial length of pole (iron)	48
Breadth of pole body across shaft	18
Diameter of shaft (minimum)	17

FIELD COPPER

Total length of winding space	16
Number of bobbins per pole	2
Axial length per bobbin	7.25
Number of winding sections per bobbin	2
Number of winding sections per pole	4
Number of turns per pole	240
Number of turns per section of winding	60
Nature of winding — copper strip wound flat.	
Width of strip	3.0
Thickness of strip	0.093
Insulation between turns	0.01
Depth of winding	6.18
Insulation on bobbin walls	0.15
Insulation between sections	0.15
Thickness of bobbin cheeks	0.40
Total cross section of winding space per pole	31.6 sq. cm.
Total cross section of copper	67.5
Space factor	0.82
Current at full load and $\cos \phi = .8$	62 amperes
Current density — amperes per sq. cm.	220
Resistance of total field at 60° C.	1.216 ohms
Volts across field at 62 amperes	75 volts

MAGNETIC DATA

Armature flux per pole (500 volts)	12.3 megalines
Leakage coefficient (calculated)	1.13
Flux in the pole core	13.9 megalines

SPECIFICATION FOR 650 KVA. 3-PHASE ALTERNATOR — *Continued.*

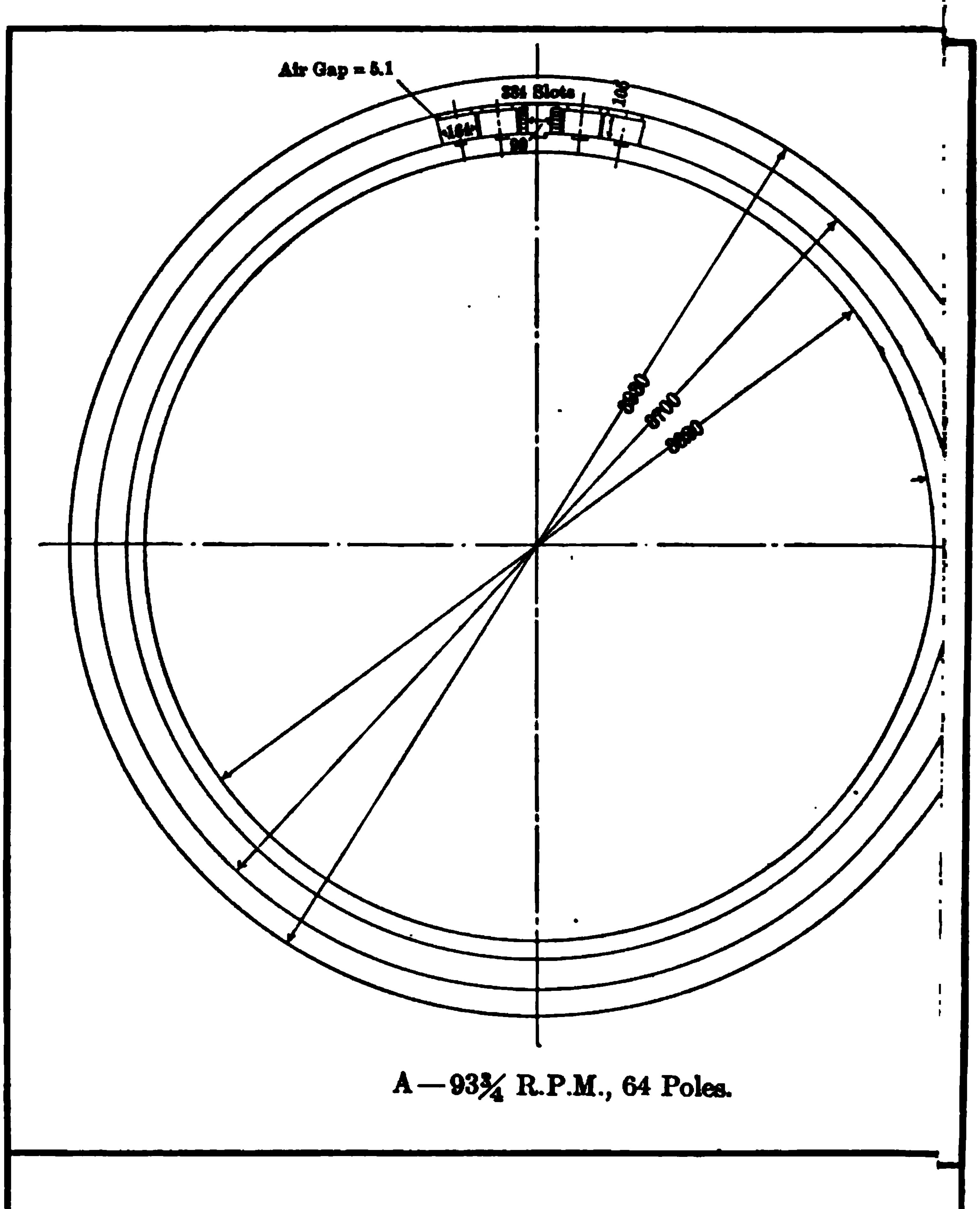
MAGNETIC DENSITIES	*C.G.S. lines per sq. cm.*
Armature	8,000
Teeth	15,500
Pole core	15,900
Hub (at minimum section)	17,500
At pole face	6,750

LOSSES AND EFFICIENCY	
I. At full load (650 kva.) *and power factor* = 1.0	kw.
Armature copper loss	3.54
Armature iron losses	17.10
Field copper loss	2.30
Total calculable losses	22.94
Assumed for friction, windage, etc.	10.00
Total losses	32.94
Efficiency	95.2%
II. At full load (650 kva.) *and power factor* = 0.8	
Total calculable losses	24.90
Total losses	34.90
Efficiency	95%

PRESSURE REGULATION	
At full load (650 kva.) and power factor = 1.0	7.5%
At full load (650 kva.) and power factor = 0.8	22%

WEIGHTS AND COSTS OF EFFECTIVE MATERIAL.

	Weight in Tons.	Cost in Dollars.
Armature Iron	2.13	270
Field Iron	1.10	140
Total Iron	3.23	410
Armature Copper	0.306	190
Field Copper	0.404	250
Total Copper	0.71	440
Total Effective Material	3.94	850
Total Effective Material per kw	0.006	1.3



A — 93¾ R.P.M., 64 Poles.

CHAPTER VIII.

INFLUENCE OF THE SPEED AND THE NUMBER OF POLES ON DESIGNS FOR 400 KVA. ALTERNATORS.

In this chapter we have brought together designs for 400 kva. 50-cycle alternators for various rated speeds. By considering so small a rating as 400 kva. we are able to include a design for 2 poles at 3000 R.P.M. The speed preferred by steam turbine manufacturers for this rating of 400 kva. would be 3000 R.P.M. The turbine speeds for machines of much larger rated output are less than 3000 R.P.M., and if we had taken a larger rating we should not have been able to include a 2-pole design. The latter design will afford an opportunity for a critical discussion of 2-pole alternators.

Below are given specifications and drawings for 400 kva. 3-phase 50-cycle alternators for the following speeds and numbers of poles:

	A	B	C
Speed (R.P.M.)	$93\frac{3}{4}$	1500	3000
Number of poles	64	4	2

The first two designs afford a comparison and contrast between a very low speed and a fairly high speed design. The third design shows the effect of still further increasing the speed up to the maximum possible speed for 50 cycles. By way of investigating the relative merits and demerits of a rotating field and a rotating armature design for the 2-pole machine, we have worked out a rough design for a rotating armature machine, and this is entered in column D of the specification.

Designs A and B are for 3000 terminal volts, while C and D are for 550 volts. This circumstance only affects the number and size of the armature conductors and the thickness and design of the slot insulation. It has no appreciable effect on the total weight of armature copper or on the general comparisons between the designs.

The procedure in carrying out the two designs A and B is substantially the same as that outlined in Chapter VII. The 2-pole design is a much more difficult problem, and design D differs in that it has

FIG. 79.— 400 kva. 4-pole 1500 R.P.M. 50-cycle 3000-volt alternating current generator.

a rotating armature. The general comparisons which we made in Chapter VI may be applied to designs *A* and *B*. Further comparisons lead to the curves in Figs. 80–86.

These four designs are arranged side by side in the following specifications. Outline drawings of the four machines are brought together in Fig. 78. The four designs have, in Fig. 78, been purposely drawn to the same scale to bring them into sharp contrast. A more detailed drawing of the 4-pole design (B), showing the general principles of the mechanical construction, is given in Fig. 79.

SPECIFICATION FOR

400 kva. 3-Phase. 64, 4, 2, 2-Pole.

94, 1500, 3000, 3000 R.P.M. 50-Cycles. 3000, 3000, 550, 550 Volt

ALTERNATING CURRENT GENERATORS.

All dimensions in cms.

	A.	B.	C.	D.
Output in kw.	400	400	400	400
Output in kva.*	400	400	500	500
Terminal voltage	3,000	3,000	550	550
Style of connection	Y	Y	Y	Y
Current per terminal	77	77	420 (525)	420 (525)
Speed . . . R.P.M.	94	1,500	3,000	3,000
Frequency	50	50	50	50
Number of poles	64	4	2	2
Armature Iron				
Diameter at air gap . . . D	370	75	63.5	55
Diameter at bottom of slots	378.4	81.6	69.1	49.4
External (internal) diameter of laminations	393	120	114	23
Gross length between coreheads . λg	18	47	66	70
Number of ventilating ducts	2	8	10	11
Width of each duct	1.27	1.27	1.5	1.5
Percentage insulation between stampings	10%	10%	10%	10%
Effective core length (iron) . . . λn	14	33	46	48.2
Width of the end ducts	1.27	1.27	1.5	1.5
Pole pitch at armature face	18.2	59	100	86.4

* Designs A and B are for 400 kw. at power factor 1.0, i.e., 400 kva.

Designs C and D are for 400 kw. at power factor 0.8, i.e., 500 kva.

The *numbers given in brackets* in columns C and D relate to the 500 kva. rating.

The comparison is thus to the disadvantage of the 3,000 r.p.m. bipolar design, but as the 3,000 r.p.m. design is very constrained it is not much larger than a liberally designed 400 kva. machine, and hence the general comparisons are not affected to any serious extent.

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.
Slots and Teeth				
Total number of slots	384	48	48	54
Type of slot	Open	Open	Open	Open
Slot pitch at armature face	3.03	4.92	4.15	3.2
Width of slot	1.45	2.44	2.28	1.7
Width of slot opening	1.45	2.44	2.28	1.7
Width of tooth at armature face	1.58	2.48	1.87	1.5
Radial depth of slot	4.2	3.3	2.8	2.8
Armature Copper				
Number of slots per pole per phase	2	4	8	9
Number of conductors per slot	8	9	1	1
Section of conductor (true). sq. cms.	0.228	0.265	3.2	2.24
Current . . . Amps.	77	77	420 (525)	420 (525)
Current density — amperes per sq. cm.	340	290	131 (164)	188 (235)
Dimensions of conductor bare	1.3×0.175	5.8 dia	2.0×0.4 4 in parl.	1.4×0.4 4 in parl.
Thickness of slot insulation	0.254	0.254	0.125	0.125
Number of turns in series per phase	512	72	8	9
Weight of copper per phase	0.113	0.054	0.10	0.064
Total weight of armature copper (Metric tons)	0.34	0.163	0.30	0.192
Armature strength. Ampere turns per pole	1850	4,150	5,050 (6,300)	5,670 (6,8000)
Rotor Iron				
Diameter at pole face	369	71.96	56.5	61.0
Length of air gap	0.51	1.52	3.5	3.0
Pole pitch at pole face	18.1	56.5	89.0	96.0
Circumferential length of pole arc	12.7	32.5	54.6	47.5
Ratio $\frac{\text{pole arc}}{\text{pole pitch}}$ at pole face	0.70	0.575	0.615	0.495
Gross axial length (parallel to shaft)	18	47	66	60
Effective axial length of pole (iron)	16.2	47	66	60
Breadth of pole body parallel shaft	9.0	17.5	22	25
Field Copper				
Total length of winding space	9.5	12.6	20.0	15.0
Number of turns per pole	40	300	156	...
Nature of winding	Strip	Sq. wr.	Sq. wire	...
Width of strip	3.18	0.44	0.635	...
Thickness of strip	0.18			...
Insulation on bobbin walls	0.5	0.5	0.5	0.5
Thickness of bobbin cheeks	0.5	0.5	0.5	0.5
Total cross section of winding space per pole	30	80	84	150
Total cross section of copper	22.6	57	63	113

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.
FIELD COPPER — *continued*				
Space factor	0.75	0.75	0.75	0.75
Current at full load and cos $\phi = 0.8$	125	38.2	128	...
Current density amps. per sq. cm.	220	200	315	150
Resistance of all field spools in series at 60° C.	0.67	2.1	0.197	...
Volts across fields at above amps.	80	80	40	...
Exciter voltage	110	110	60	...
Weight of copper per spool . . tons	0.0143	0.080	0.112	0.216
Total weight of copper in all field spools . . . tons	0.920	0.325	0.224	0.432
MAGNETIC DATA. Volts per phase				
Armature flux per pole (at normal voltage) (megalines)	1.6	10.2	17.0	14.85
Leakage coefficient (calculated at no load)	1.3	1.17	1.35	1.1
Flux in the pole core	2.1	12.0	22.0	16.4
MAGNETIC DENSITIES in lines per sq. cm.				
Armature	7,800	8,150	8,200	11,800
Teeth (corrected)	16,000	17,200	11,000	15,600
Pole core	14,400	14,700	15,100	11,000
Yoke	6,000	12,300	...	13,000
At pole face	7,000	6,100	7,350	...
Mean density in air gap	7,800	6,800	5,660	4,950
AMPERE TURNS				
Armature	40	120	225	175
Teeth	105	130	17	59
Gap	8,190	8,200	16,000	11,860
Pole core	170	225	350 (pole core and yoke)	212
Yoke	330	100		820
Total	3,835	8,775	16,590	13,120
Iron ampere turns in per cent of total	17	6.0	3.7	10
Gap ampere turns in per cent of total	83	94	96.3	90
LOSSES				
Armature Copper				
Length of mean turn (metres)	1.08	3.20	4.32	3.56
Resistance per phase at 60° C.	0.485	0.174	0.00215	0.00286
Total I^2R loss at full load cos $\phi = 1$ kw.	8.7	3.1	1.14 (1.78)	1.51 (2.37)
Armature Iron				
Weight of armature iron (excluding teeth) . . . tons	0.96	1.54	2.32	0.560
Frequency	50	50	50	50
Flux density — kilolines per sq. cm.	7,800	8,150	8,200	11,800
Kw. per ton	6.8	7.3	7.4	15.3
Total core loss . . . kw.	6.5	11.2	17.2	8.57

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.
Teeth				
Weight of teeth . . . . . . tons	0.29	0.11	0.10	0.075
Flux density	16,000	17,200	11,000	15,600
Kw. per ton	27	32.6	13.2	26.8
Total tooth loss . . . . kw.	7.8	3.6	1.32	2.0
Total iron loss	14.3	14.8	18.52	10.57
Iron loss + copper loss . . . . kw.	23.0	17.9	19.66 (20.30)	12.08 (12.94)
Field Copper				
Excitation power at full load $\cos\phi = 1$ . . . . kw.	6,800	1,810	3,700	. . .
Excitation power at full load $\cos\phi = 0.8$ . . . . kw.	10,000	2,920	5,150	2,200
Watts per sq. dcm. of external surface of field spool $\cos\phi = 1$ . .	19.7	35	33	9
ARMATURE HEATING COEFFICIENTS				
Watts per sq. dcm. of armature surface $\cos\phi = 1$				
(A) Calculated on $\pi(D\lambda g + 0.7\tau)$ . .	64	86	78	51
(B) Calculated on $\pi D\lambda g$	81	135	143	88
(C) Calculated on total surface . . .	37	42	39	57
EFFICIENCY (excluding bearing friction and windage.)				
Armature copper loss . . . . kw.	8.7	3.1	1.14 (1.78)	1.51 (2.37)
Armature iron loss . . . . kw.	14.3	14.8	18.5	10.6
Field copper loss . . . . kw.	6.8	1.8	3.70	2.2
Total electric and magnetic losses kw.	29.8	19.7	23.34 (23.98)	14.31 (15.2)
Efficiency — full load $\cos\phi = 1$ . . .	92.2	94.5	94.5	96.5
Efficiency — half load	89.5	92.0	92.6	95.0
Inherent regulation at full load and $\cos\phi = 1$	5%	3%	3.5%	3.0%
At full kva., and $\cos\phi = 0.8$	18%	18.6%	16% (19%)	14% (18%)
CONSTANTS AND COEFFICIENTS				
Weight of active material . . . tons	4.92	3.14	3.72	3.81
Weight of active material per kva. kg.	12.3	7.85	9.3	9.5
Cost of active material . . . . . . $	1,192	635	727	787
Cost of active material per kva. $	3.0	1.59	1.82	1.97
$D \times \lambda g$ metres . . . . $D\lambda g$	0.67	0.35	0.42	0.385
$D^2\lambda g$ metres . . . . $D^2\lambda g$	2.46	0.25	0.266	0.212
Ratio $\frac{\lambda g}{\tau}$	1.0	0.83	0.745	0.78
Ratio $\frac{D}{\lambda g}$	21.0	1.6	0.96	0.79

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.
CONSTANTS AND COEFFICIENTS — *cont'd*				
Output coefficient . . . . ξ	1.73	1.06	0.5 (0.625)	0.63 (0.79)
Ampere conductors per cm. of periphery . . . . α	203	140	100 (126)	130 (163)
Flux (no load) per sq. cm. of armature surface . . . . β	6,300	5,250	2,570	2,450
Peripheral speed (metres per sec) . s	18.1	56.5	88	86.5
Watts per c. cm. of active belt . . .	4.5	10.5	13.0	14.1
Ratio of (no load) field ampere turns to armature ampere turns	2.05	2.1	3.2 (2.6)	2.4 (1.9)
Ratio of short circuit to full load current	3.0	3.0	4.5 (3.7)	3.5 (2.8)
Kva. per pole	6.25	100	200 (250)	200 (250)
Estimated total net weight T.W. tons	11.1	5.5	6.5	4.56
Weight coefficient $\frac{\text{T.W.}}{D\lambda g}$	16.6	15.7	15.5	11.8
Weight coefficient $\frac{\text{T.W.}}{D^2\lambda g}$	4.5	22.0	24.4	21.5

WEIGHTS AND COSTS OF EFFECTIVE MATERIAL.

	Weight in Tons.				Cost in Dollars.			
	A.	B.	C.	D.	A.	B.	C.	D.
Magnet cores . . . . . . Magnet shoes	0.96	0.51	0.78	0.70	120	64	97	87
Magnet yoke	1.45	0.50		1.85	128	62		231
Armature laminations . .	1.25	1.65	2.42	0.64	156	206	303	79
Effective iron (total) . .	3.60	2.66	3.20	3.19	404	332	400	397
Armature copper	0.34	0.16	0.30	0.19	212	103	187	120
Field copper	0.92	0.32	0.22	0.43	576	200	140	270
Effective copper (total) .	1.26	0.48	0.52	0.62	788	303	327	390
Total effective material .	4.92	3.14	3.72	3.81	1192	635	727	787

In Figs. 80 to 86, are plotted the various quantities which it is of interest to study as a function of the speed.

Fig. 80 shows the values of $D^2\lambda g$ and $D\lambda g$. The points have been joined up into smooth curves, as designs lying between 93¾ R.P.M.

and 1500 R.P.M. would lie somewhere near such curves. The curves have been continued between the 1500 and 3000 R.P.M. points, although there is strictly no point lying between these two speeds, as there is no utilizable number of poles between 2 and 4.

Fig. 81 shows the output coefficient ξ and the coefficients α and β, the ampere conductors per centimetre, and the flux per square centimetre of armature periphery respectively. The output coefficient obtained with the 2-pole design is much lower than that for the 4-pole

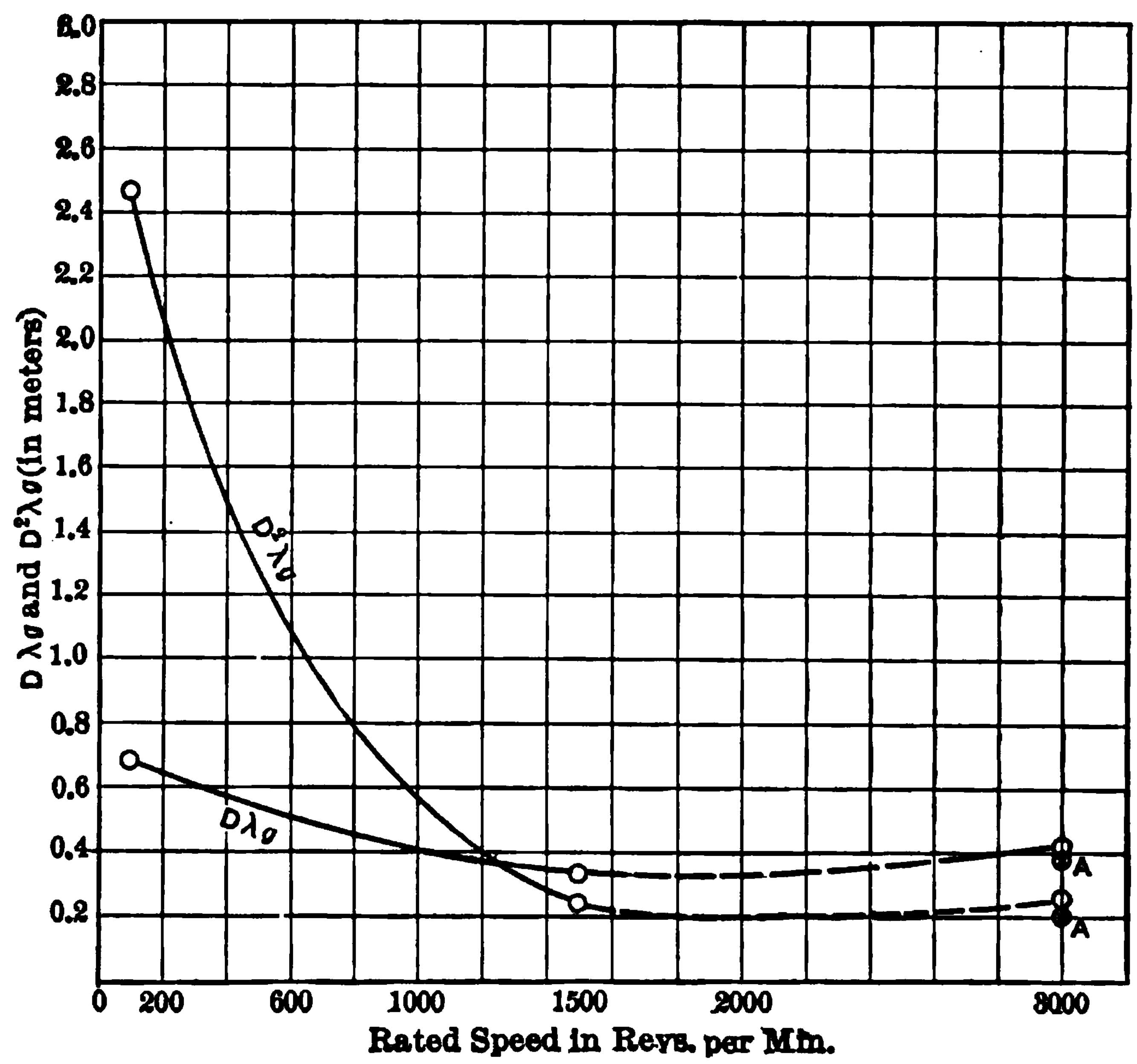


FIG. 80.— $D\lambda g$ and $D^2\lambda g$ for 400 kva. alternators.

design, the latter being about 50 per cent higher than the former. Thus, although the speed is double, the value of $D^2\lambda g$ is practically the same in the two cases. From Fig. 80 we see that the value of $D^2\lambda g$ rises rapidly in the lower speeds when the speed of 1500 R.P.M. is passed. The value of $D\lambda g$ does not vary nearly so widely, and as we shall see later, this is of interest in connection with the weight coefficients to which reference was made in Chapter II.

The points marked A in these figures, represent the design D for a rotating armature machine. We shall reserve to a later stage a thorough discussion of design C.

The curves for α and β in Fig. 81 show that both these quantities decrease at the high speeds. This is to be expected, since the product of α and β is proportional to ξ (see Chapter II), and ξ is decreasing. The low values for ξ, α and β in the 2-pole machine, are directly due to the very high speed. The values of D and λg cannot be further decreased, since the heating coefficients would become excessive, and

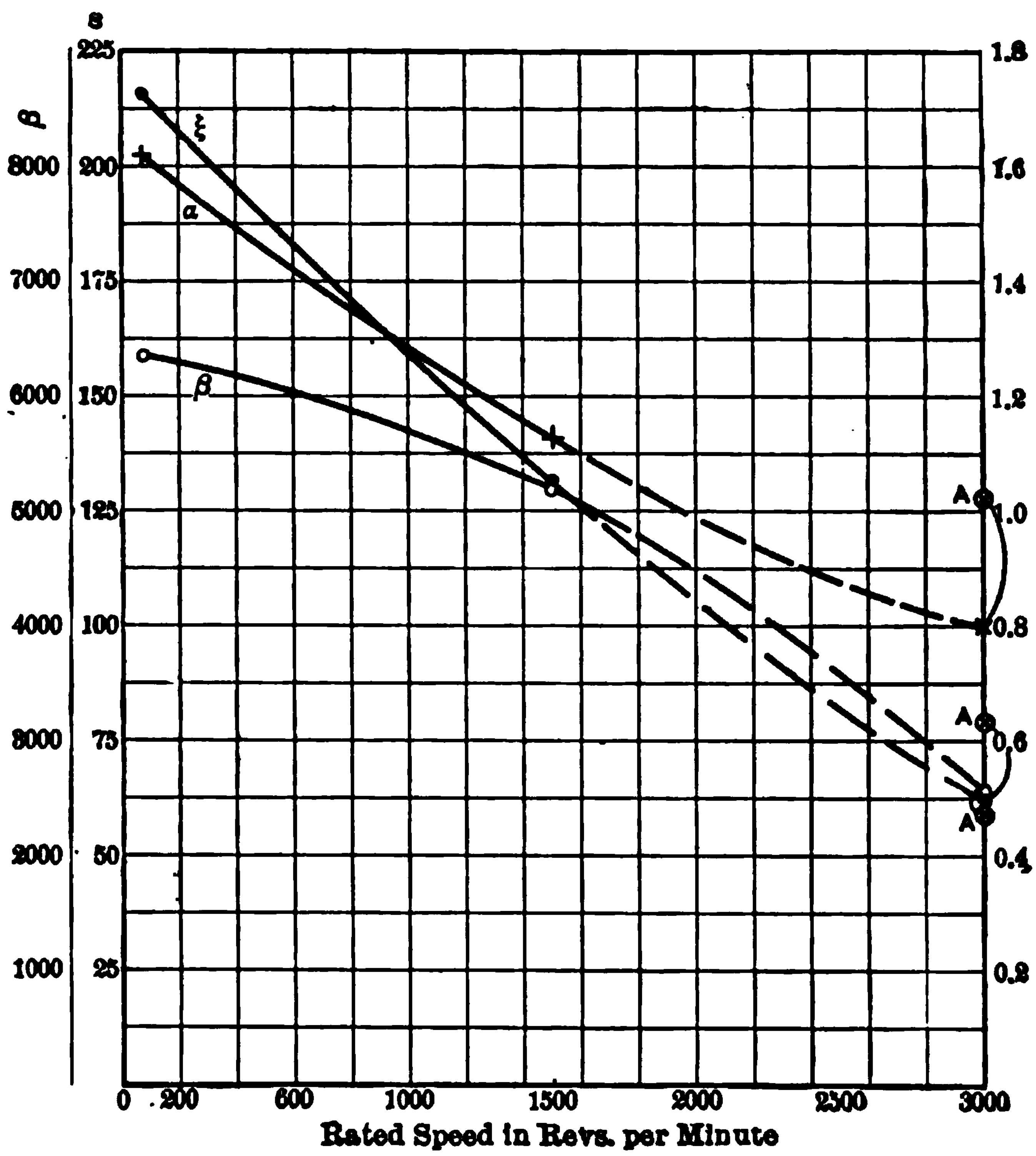


FIG. 81.— Specific electric and magnetic loadings α and β and output coefficient ξ for 400 kva. alternators.

there would not be sufficient space for the field winding. These circumstances necessitate a large value for D and λg, and consequently of $D^2 \lambda g$, and a low value for ξ. For the same reasons the 4-pole design has a lower value for ξ, although the difficulties here are not nearly so accentuated as for the 2-pole design.

Losses and Efficiency.— The curves in Fig. 82 show the various losses plotted individually. The total electrical and magnetic losses

decrease and the efficiency increases from the low speeds up to 1500 R.P.M., but beyond this design, the losses in the 2-pole 3000 R.P.M. machine are considerably greater.

It is interesting to investigate this point from the individual curves. First, the armature iron loss increases slowly with the speed until the 4-pole design is reached, and the iron loss in the 2-pole machine

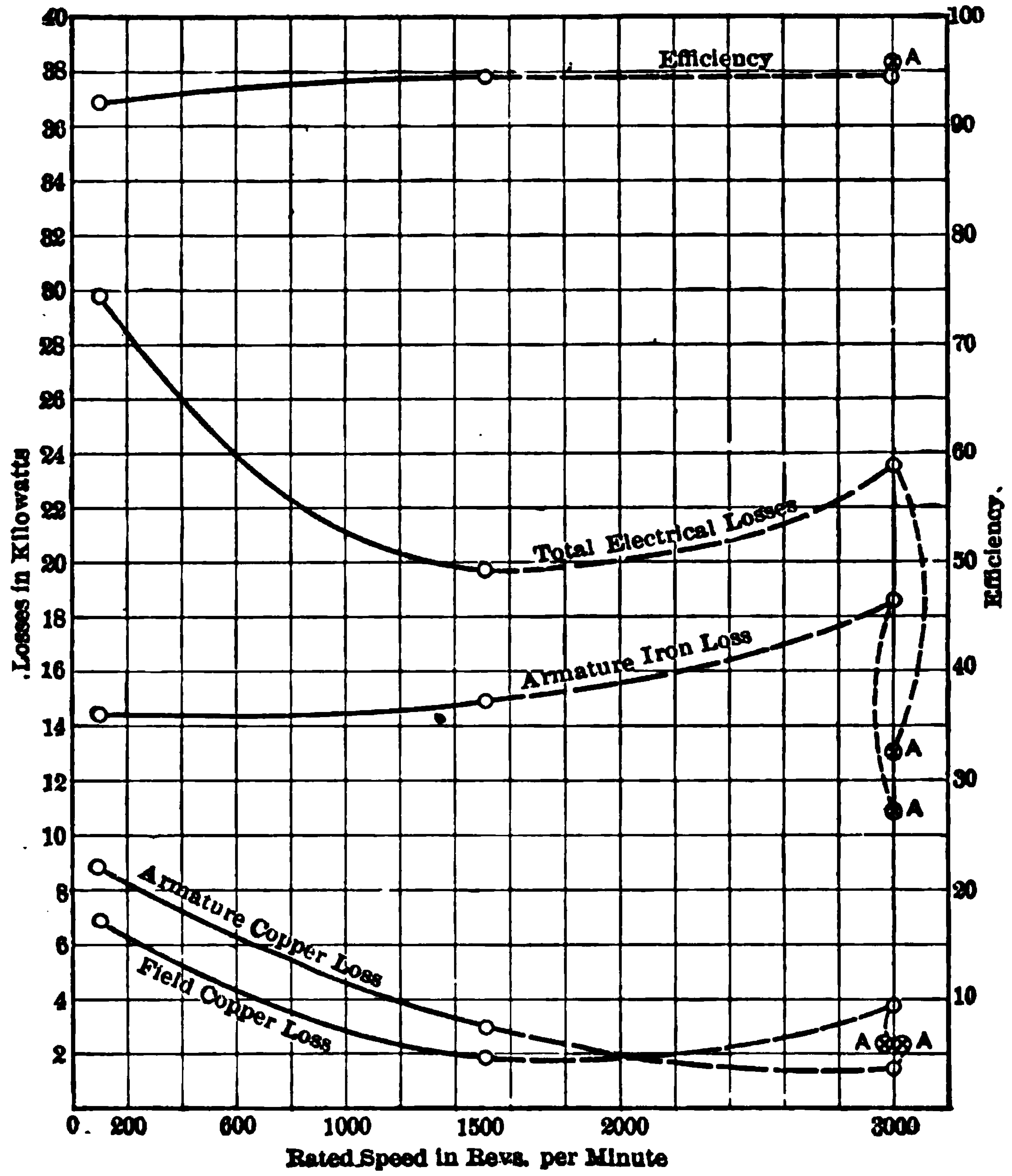


FIG. 82.— Losses and efficiencies for 400 kva. alternators.

is considerably higher. This is due to the large radial depth of the laminations, and their consequent great weight. These losses may be considerably reduced by the employment of the special grade low loss iron referred to in Chapter IV. We have in that chapter considered the improvements which may be effected in the design by using an iron of this character, and have seen that the principal drawback is the increase in the Total Works Cost of the machine. This increase

in the Total Works Cost has, however, been shown to be by no means prohibitive.

Secondly, the field copper loss decreases with increasing speed on account of the smaller amount of field copper required. Here again we find that the 2-pole design has a much higher field copper loss. This is due to the very limited winding space available in such a design. The most difficult problem in designs for very high speed, relates to the field winding. The space available is dependent on the air gap diameter, and this is limited by peripheral speed restrictions. The current density, losses and heating in the field are thus necessarily pushed very high, as will be seen on pages 134 and 136 of the specification for the 400 kva. designs.

The armature copper loss decreases with increase of speed on account of the decreased total length of conductor. At this rating there is no restriction as to slot space in the armature. Thus we see that the 2-pole design has a considerable disadvantage in connection with the field heating, which is further emphasised by the necessity for a higher ratio of field ampere turns to armature ampere turns, as we shall see immediately in connection with the pressure regulation.

On account of the restricted diameter, the ventilation of the rotor becomes a very difficult problem in the 2-pole designs.

Pressure Regulation. — If the machines are designed to the same limits of pressure regulation, the ratio of field ampere turns to armature ampere turns must be considerably higher in the case of the 2-pole design. This is a consequence of the lower saturation of the 2-pole design, as only a comparatively small proportion of the field ampere turns can be expended on the iron parts of the magnetic circuit.

We have explained in Chapter V the relation between these quantities, and the present cases may be taken as concrete examples of the conclusions there drawn. The machines with a low degree of saturation have the advantage that for a given regulation at 0.8 power factor, the regulation at unity power factor is closer, but this is not sufficient to counterbalance the large expenditure for field copper. If in the case of the 2-pole design, the field copper losses are reduced by employing a lower ratio of field ampere turns to armature ampere turns, the regulation is seriously impaired.

In Fig. 83 we have plotted the ratio of field ampere turns to armature ampere turns, and also the air gap ampere turns and the iron ampere turns as percentages of the total field ampere turns. It is seen

that for the 2-pole design only 4 per cent of the total ampere turns are expended on the iron, and the field ampere turns per pole have to be 3.2 times the armature ampere turns.

In Fig. 84 are plotted the armature strength, field strength, and depth of air gap. If all the machines had the same degree of saturation, the air gap depth would be proportional to the armature strength. As the higher speed designs are less saturated, the air gap depths are greater than would otherwise be necessary, and consequently the depth of the air gap increases with the speed more rapidly than the armature strength increases.

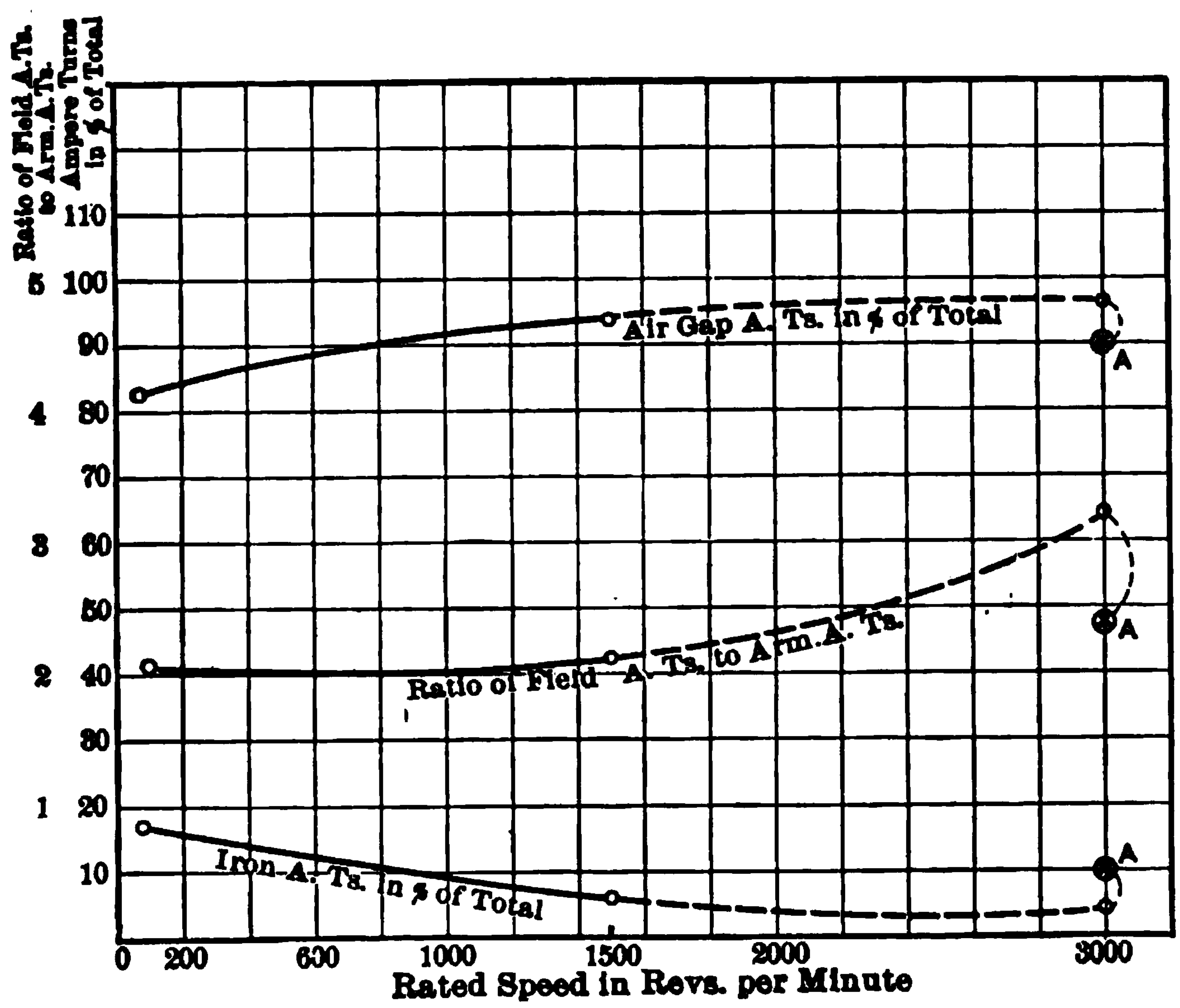


FIG. 83.— Air gap and iron ampere turns in per cent of total field ampere turns, and ratio of field ampere turns to armature ampere turns for 400 kva. alternators.

Weight and Cost.—Fig. 85 shows the weight and cost of the effective material. It will be seen that both these quantities reach a minimum at a speed of 1500 R.P.M. corresponding to the 4-pole design. The 2-pole 3000 R.P.M. design, although of double the speed, has considerably greater weight and cost than the 4-pole 1500 R.P.M. design. The causes to which this is due will be seen from the table of component weights and costs at the end of the specification, on page 137. The chief differences are in the armature iron, which weighs 2.4 tons in the 2-pole as against 1.6 tons in the 4-pole design; and in the

armature copper, which is 0.50 ton against 0.16 ton. These are directly accounted for by the large pole pitch, which is 88 cms. in the 2-pole and 59 cms. in the 4-pole design.

In Fig. 86 are plotted the estimated total net weights. These are obtained by assuming an appropriate value for the weight factor which is taken from the curve in Fig. 10, Chapter II. The total weight of the 2-pole machine is 6.5 tons against 5.5 tons for the 4-pole machine.

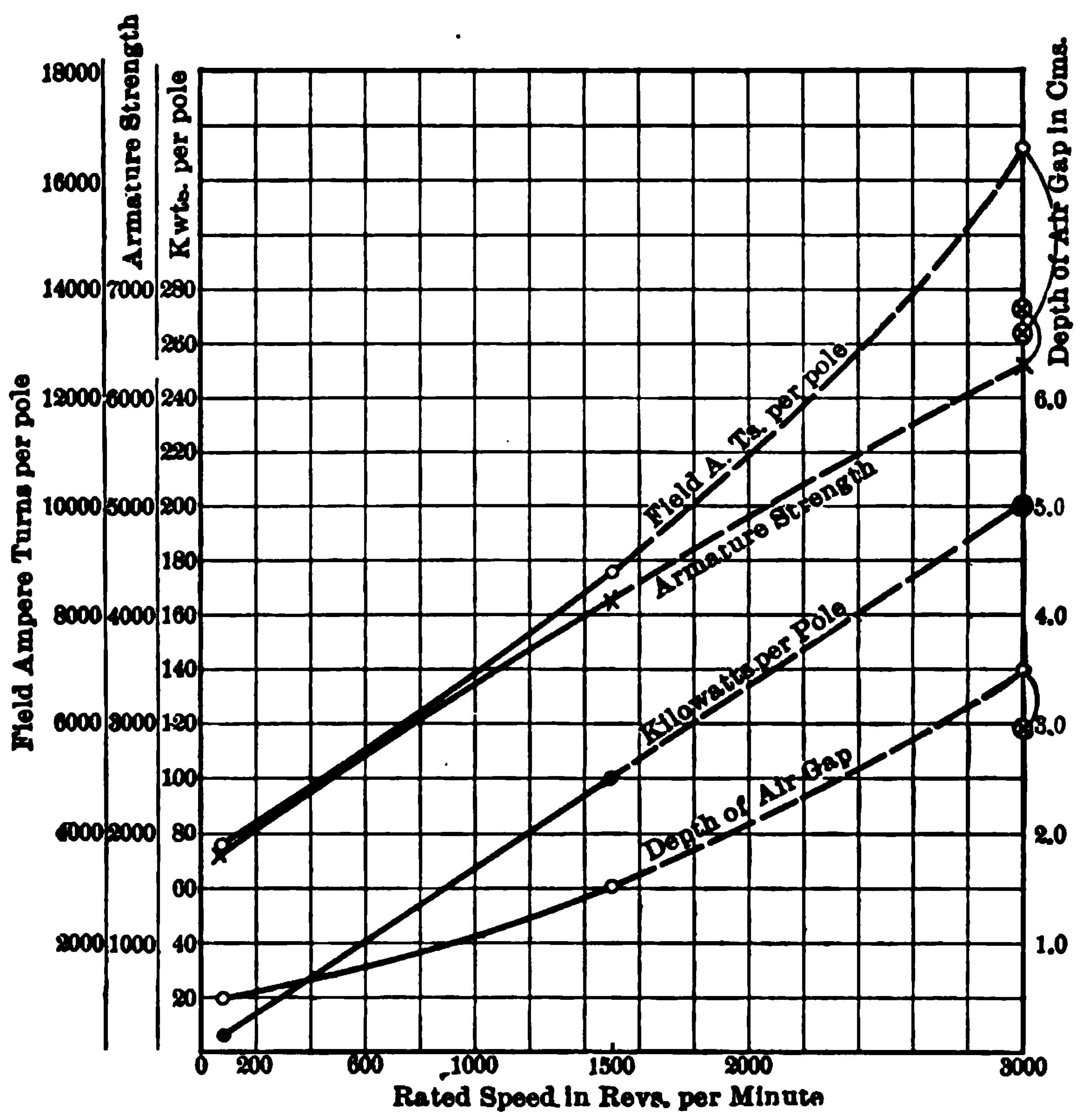


FIG. 84.—Armature and field strengths, depth of air gaps, and kilowatts per pole for 400 kva. alternators.

The 93¾ R.P.M. machine weighs 11 tons, or only 55 per cent more than the 3000 R.P.M machine. Designs for other speeds will lie somewhere near the curve which has been drawn in. Thus, a 3000 R.P.M. machine would weigh about the same as one for 1000 R.P.M.

It is interesting to compare these results with those arrived at by Behrend and already given in Fig. 66, on p. 93 of Chapter VI. Behrend's curve relates to 1000 kw. 25-cycle generators, and it is seen that

the speed for minimum weight is about 750 R.P.M., corresponding at this periodicity, to a 4-pole design. This machine weighs 17 tons, whereas a 2-pole 1500 R.P.M. design weighs 26 tons, which is about the same weight as a 250 R.P.M. design. We see that in both cases the bipolar machine is much heavier, and it is evident that the weight is more dependent on the number of poles than on the actual rated

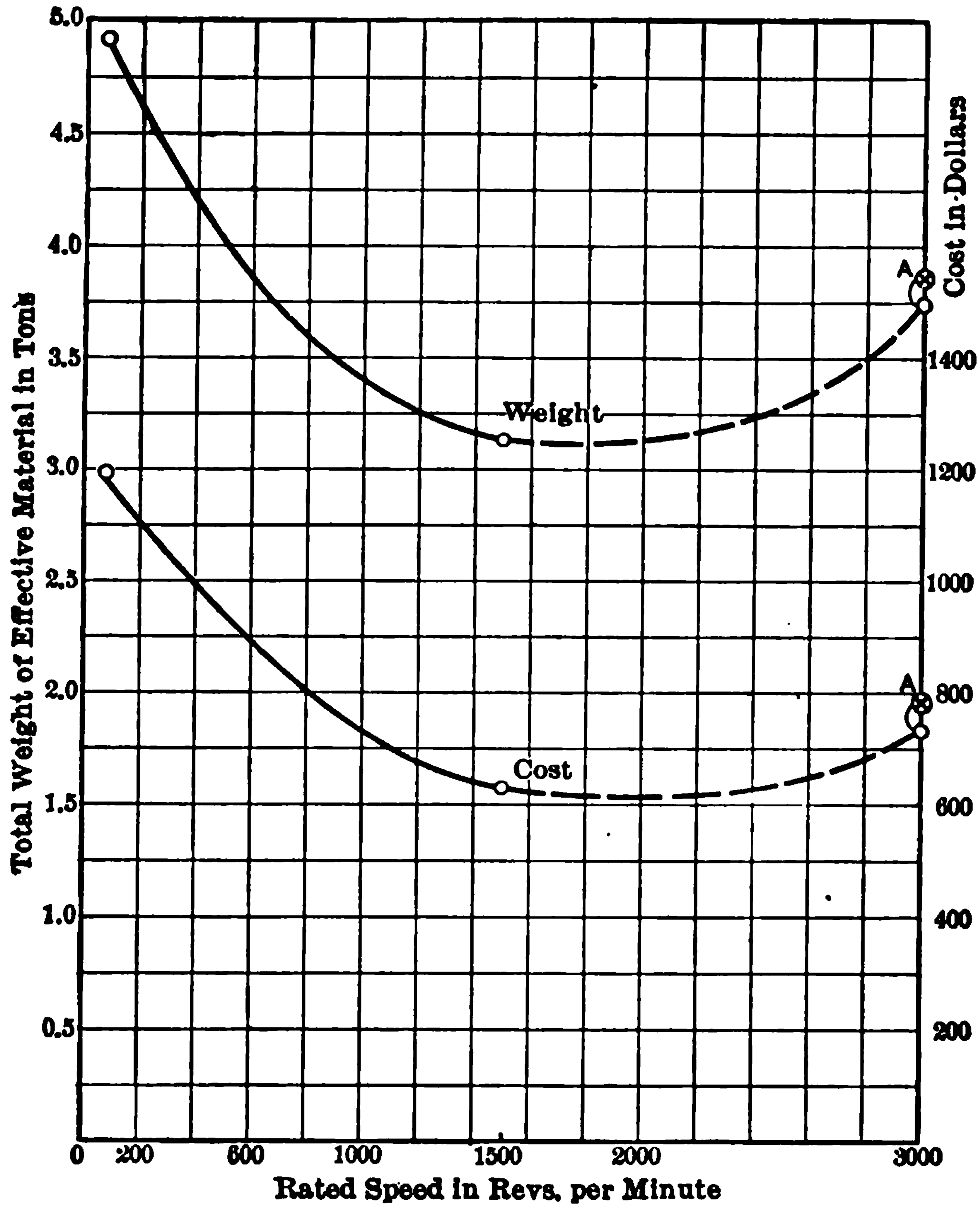


FIG. 85.— Weight and cost of effective material for 400 kva. alternators.

speed. The weight is thus dependent on the frequency associated with the speed.

A 1500 R.P.M. 50-cycle machine for 1000 kw. with 4 poles, would show a considerably less weight than 26 tons, which is Behrend's weight for a 1500 R.P.M. 2-pole 25-cycle 1000 kw. generator.

The above conclusions relating to 2-pole designs, taken in addition to their inferior electrical qualities, and the difficulties of mechanical construction and ventilation, render such designs con-

siderably inferior, and not to be preferred to designs with more poles and for slower speeds. For small rated outputs, 4-pole designs are quite favourable, and for large rated outputs it may be found that 6 poles or 8 poles are more suitable. Further reference will be made to this matter in the two following chapters.

In spite of the above considerations, there is a tendency in certain quarters to give preference to a bipolar design on account of the advantages that may be obtained by employing the higher speed *for the steam turbine.* Some manufacturers produce bipolar machines of weight and cost not greater than four-pole machines of the same

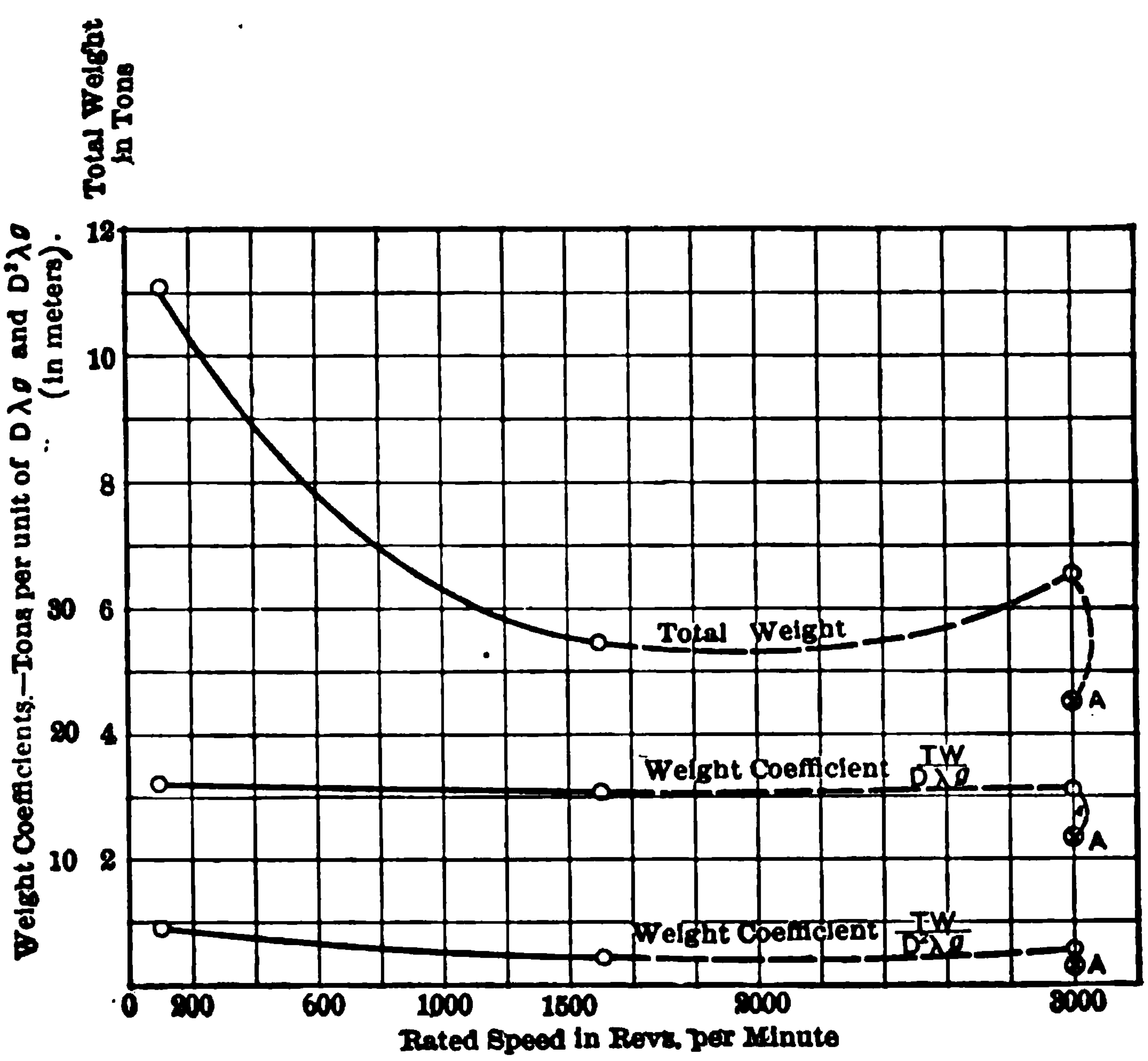


FIG. 86.— Total net weight and weight coefficients for 400 kva. alternators.

rated output, but in view of the above conclusions, it is evident that such economy is obtained only by sacrificing the operating qualities. In such designs forced draught ventilation is practically an absolute necessity in order to ensure a temperature rise within the normal limits.

By way of further illustration of the characteristics of bipolar designs, and for the purpose of comparison with the above design, there are tabulated below the principal data for three designs built respectively by the Westinghouse Co., Messrs. Brown, Boveri, & Co., and

Kolben & Co. The rated outputs and speeds of these are near those of the designs already given in this chapter.

TABLE 25.

DATA FOR BIPOLAR ALTERNATORS.

	Kolben.	Westinghouse Co.		Brown, Boveri.	Authors.
Rated output . . . . kva.	300	400		550	500
Terminal voltage	...	400		2000	550
Speed . . . . R.P.M.	3000	3600		3000	3000
Number of poles	2	2		2	2
Frequency	50	60		50	50
Diameter at air gap . . . . . . D	458	600	550	600	635
Armature gross core length . . λg	630	700	750	740	660
Output coefficient . . . . ξ	0.75	0.44	0.44	0.63	0.625
Peripheral speed (m/sec.) . . . . S	72	113	105	93	85
Pole pitch . . . . τ	720	943	865	943	88
Ratio $\lambda g/\tau$	0.87	0.74	0.87	0.78	0.74
Ratio $D/\lambda g$	0.73	0.86	0.73	0.81	0.96
$D^2\lambda g$ metres	0.13	0.25	0.23	0.265	0.27
$D\lambda g$ metres	0.29	0.42	0.41	0.44	0.42
Flux per pole megalines	18	...		...	17
Radial depth of air gap	1.2	2.5		...	3.5
No. of stator slots	...	36		...	48
No. of stator slots per pole per phase.	...	6		...	8
No. of stator ventilating ducts . . .	...	11		...	10
Width of each duct	...	11		...	15
Armature net core length . . . . λn	...	57		...	46
Ratio $\lambda n/\lambda g$	...	0.79		...	0.69
Total flux from all poles	36	...		...	34
Flux per sq. cm. of air gap surface. β	...	...		...	...
Ampere conductors per cm. . . . α	...	...		...	...
Armature strength — (ampere turns per pole)	...	...		...	...

In the machine in the second column, the field winding is distributed in 16 slots or 8 slots per pole. Each slot is 70 mm. deep and 20 mm. wide. The mouths of the slots are closed by wedges 20 mm. thick. The rotor is provided with three ventilating ducts.

The following are some test results on a 500-kva. 60-cycle 3600 R.P.M. 2-pole Westinghouse turbo-alternator. The data of these tests was published in the "Street Railway Journal" for Dec. 29, 1906, Vol. XXVIII, p. 1174. From the test figures we have deduced and plotted the characteristic curves in Figs. 87–90.

In making the iron loss test and the short circuit loss test, the turbo-alternator was belted to a continuous current motor and was driven at its normal speed of 3600 revolutions per minute. The combined efficiency of the motor and of the driving belt system was

considered as constant, hence, in this case, the iron loss and short circuit loss were obtained by deducting from the input to the motor when the alternator was running loaded, the input to the motor when it was running on no load. The remainders of course give the component losses.

TABLE 26.

CORE LOSS TEST ON 500 KVA. 60 CYCLE 3600 R.P.M. 2-POLE ALTERNATOR.

Motor.—525 Volts. 3.35 Amperes in Field.		Generator.—Speed, 3600 R.P.M.		
Amperes Armature.	Kw. Input to Motor.	A. C. Volts Armature.	Amperes in Field.	Core Loss kw.
52	27.3	0	0	0
55	28.8	395	8.0	1.6
59	31.0	795	15.25	3.8
64	33.6	1192	23.0	6.3
69	36.2	1600	30.8	8.9
77	40.5	1995	39.0	13.2
85	44.6	2300	46.5	17.3
100	52.5	2700	58.2	25.2
110	57.8	2865	65	30.5

In Table 26 are given the motor and generator readings for the iron loss test, and from these the curves of Figs. 87 and 88 have been plotted. In Fig. 87 the no load saturation curve has been plotted. This shows the voltage on open circuit as a function of the field excitation amperes. In Fig. 88 the iron loss has been plotted against the volts on open circuit.

In the determination of the iron loss for a terminal pressure of 2300 volts for different loads and at unity power factor, this loss has been taken equal to that at 2300 volts on open circuit, as the internal voltage drop, for loads within the ordinary range of loading, is in this case fairly small, and it is convenient to take the loss as independent of the load. On reference to the curve of Fig. 88, this loss is found to be equal to 17.3 kw.

Field Excitation I^2R Loss. — The excitation current required to maintain a constant terminal voltage, varies with the load and with the power factor. At no load the excitation current is that required to overcome the saturation of the magnetic circuit, but when loaded, this excitation current has to be increased by an amount equal to that required to overcome the armature demagnetisation and distortion, and also the impedance drop.

In the tests as set forth in the "Street Railway Journal," the voltage regulation at full load and unity power factor was given as 9.8 per cent, and that at full load and 80 per cent power factor as 23.6 per cent. Therefore the excitation current required to maintain normal voltage of 2300 volts on full load at unity power factor would, if the generator were working on no load or on open circuit, produce a terminal voltage equal to about,

$$2300 + \frac{23.6}{100} \times 2300 = 2850 \text{ volts.}$$

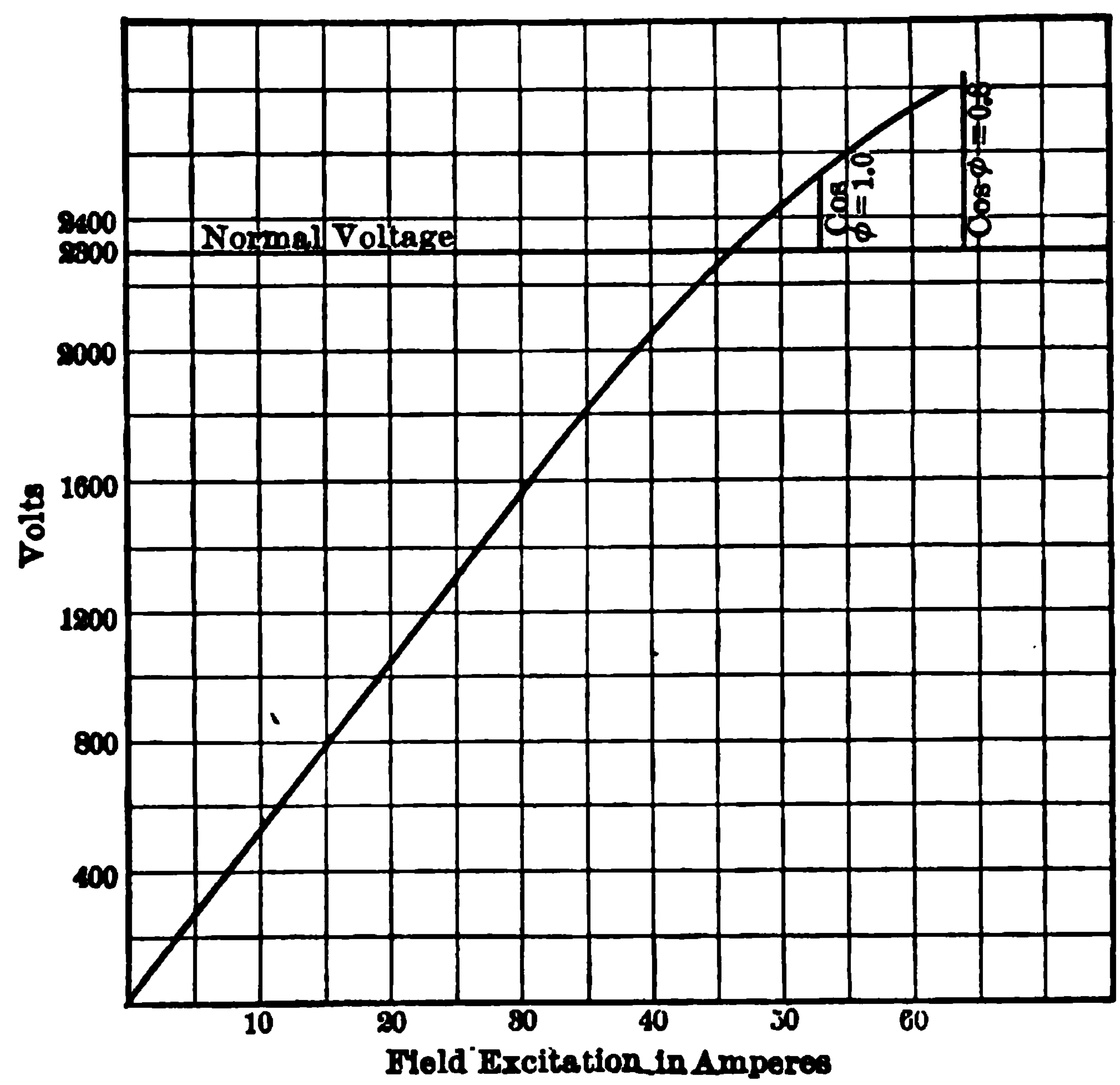


FIG. 87.— No load saturation curves of 500 kva. 3600 r.p.m. 60 cycle 2-pole 4000 volt alternator.

On reference to the saturation curve in Fig. 87, the excitation currents corresponding to these two voltages are found to be 53 and 64 amperes respectively. The excitation regulation is equal to

$$\frac{64 - 53}{53} \times 100 = 20\%$$

This value is low on account of the low degree of saturation and the large proportion of the field excitation which is expended on the air gap.

The American Institute of Electrical Engineers in its Standardisation Rules, recommends that the efficiency of an alternating current generator shall be specified as that attained at full load at unity power factor, hence the excitation current at full load should be taken as 53 amperes.

The resistance of the field circuit at the temperature corresponding to continuous operation is given as equal to 1.48 ohms. The field I^2R loss is, therefore, at full load, equal to,

$$1.48 \times (53)^2 = 4.15 \text{ kw.}$$

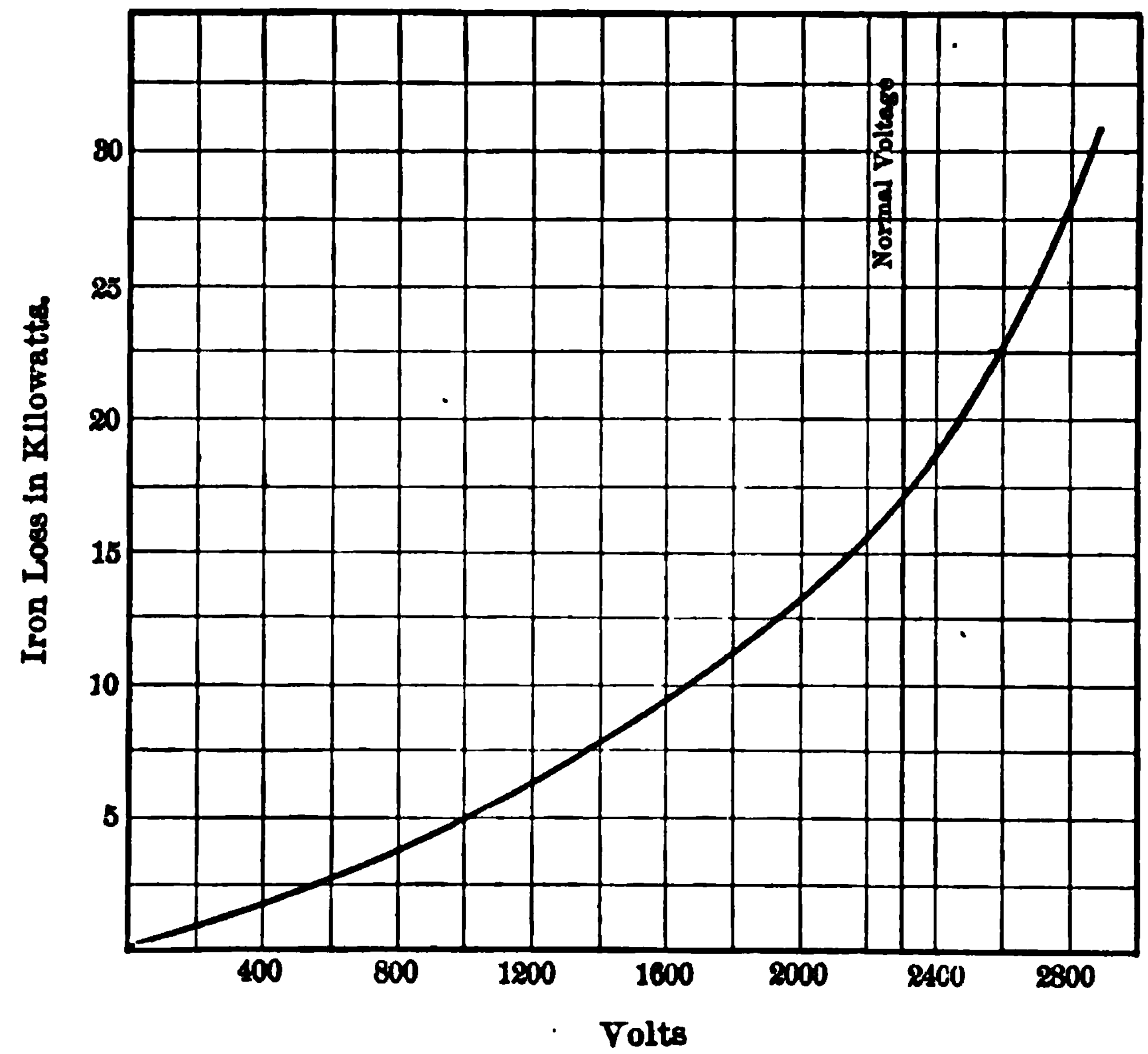


FIG. 88.— Curves of iron loss for various voltages on open circuit for 500 kva. 3000 r.p.m. 60 cycle 2-pole 4000 volt alternator.

At no load the field excitation current required for a normal terminal voltage of 2300 volts is found, by reference to Fig. 87, to be equal to 46.5 amperes. The field I^2R loss is, therefore, at no load, equal to $1.48 \times (46.5)^2 = 3.2$ kw.

In the Standardisation Rules above referred to, it is recommended that the I^2R loss in the field regulating rheostat be charged to the whole plant and not to the generator considered separately.

In Table 27 are given the motor and generator readings for the short circuit loss test, and from these the curve (a) of Fig. 89 has

been plotted. This shows the relation between the short circuit loss in kilowatts and the armature amperes per phase. The total

TABLE 27.

SHORT CIRCUIT TEST ON 500 KVA. 60 CYCLE 3600 R.P.M. 2-POLE ALTERNATOR.

Motor.—525 Volts. 3.35 Amperes in Field.		Generator.—Speed, 3600 R.P.M.		
Amperes in Armature.	Kw. Input to Motor.	Amperes in Armature per Phase.	Amperes in Field.	Total Loss on Short Circuit.
52	27.3	0	0	0
55	28.8	64	12.2	1.5
61	32.3	127	24.5	5.0
66	35.7	154	30.2	8.4
77	40.5	191	37.4	13.2
10	5.0	Belt off		...

short circuit loss for various armature currents as entered in Table 27, and plotted in curve (*a*) Fig. 89, includes the armature I^2R loss.

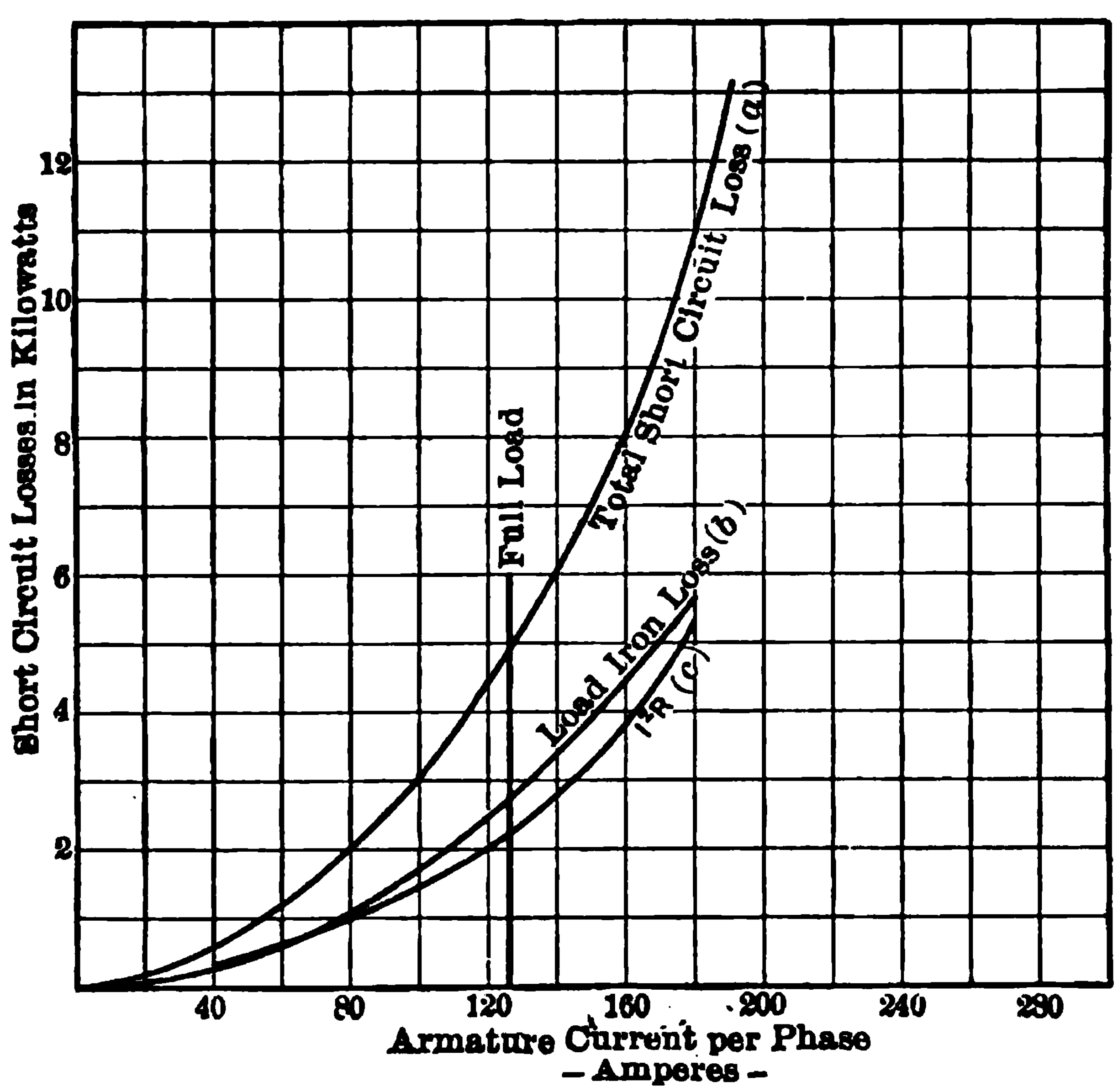


FIG. 89.— Curves of short circuit, armature I^2R, and load iron loss on short circuit for 500 kva. 3000 r.p.m. 60 cycle 2-pole 400 volt alternator.

The curve (*c*) has been drawn showing the total calculated armature I^2R loss plotted against the armature amperes per phase.

The differences between these two losses at various armature currents are represented by the curve (*b*) in Fig. 89. Curve *b* therefore represents the load core loss of the alternator when running under short circuit conditions.

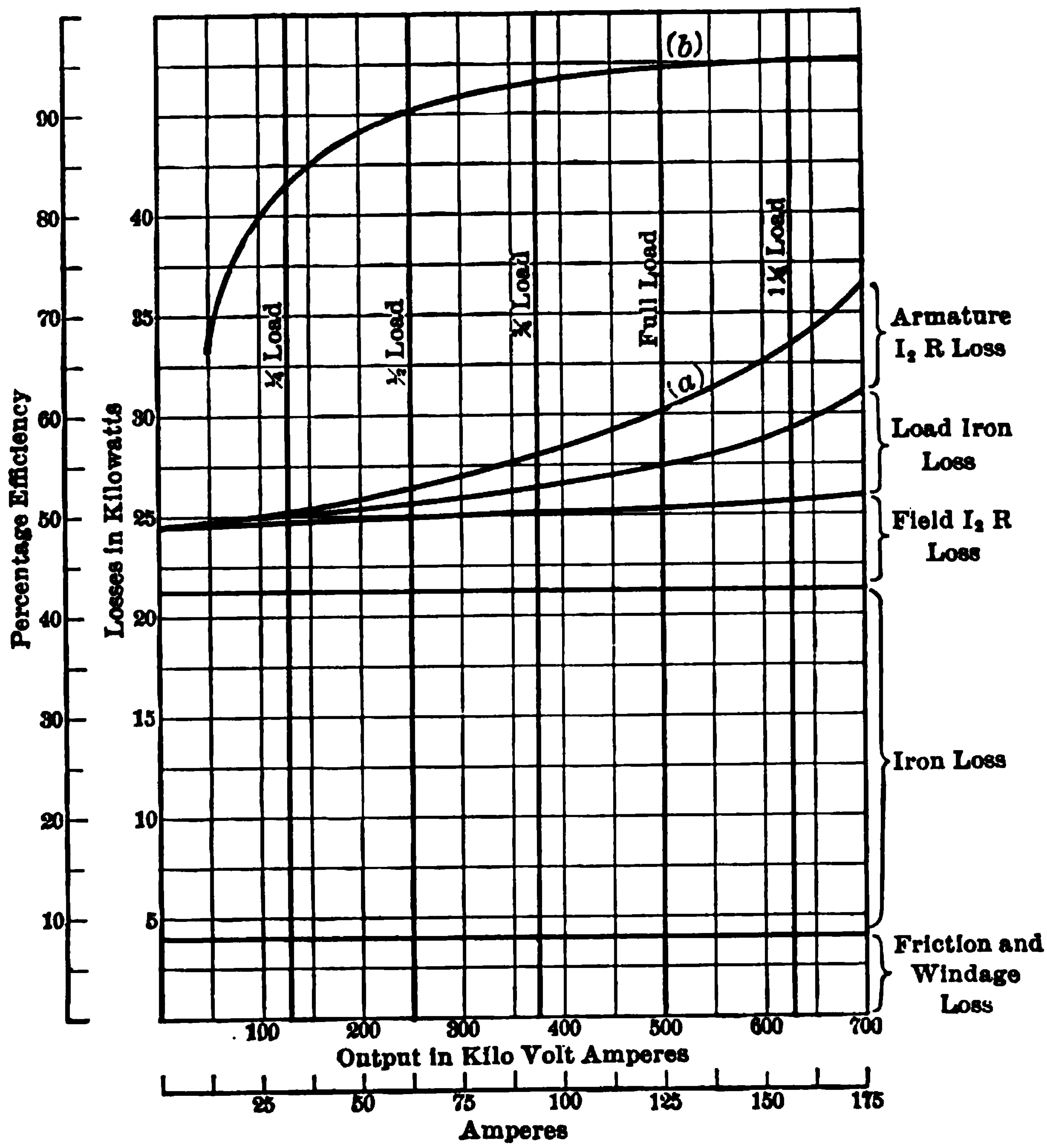


FIG. 90.— Curves for losses and efficiency of 500 kva. 3600 r.p.m. 60 cycle 2-pole 400 volt alternator.

The load core loss under working conditions for various armature currents has, as already mentioned, been taken at the value corresponding to short circuit conditions for the same armature current.

The friction loss was not determined in these tests. It has here been assumed to have been equal to 4 kilowatts (i.e., to eight-tenths of one per cent of the rated output).

In Fig. 90, the component losses have been brought together and plotted, for a constant terminal pressure of 2300 volts, as a function of the load in kilovolt-amperes at unity power factor and of the amperes per phase. The machine has an air gap depth of 3.8 centimetres ($1\frac{1}{2}$ inches). The values obtained for the pressure regulation on full load at power factors 1.0 and 0.8 are set forth in Table 28:

TABLE 28.

Regulation Tests on 500 kva. Bipolar Alternator.

Power Factor.	Voltage.	Regulation Per Cent.	
		Machine I.	Machine II.
1.0	2300	9.8	10.6
1.0	2000	14.0	16.0
0.8	2300	23.6	25.6
0.8	2000	33.0	34.5

These figures are very poor compared with the standards given in Chapter V, and they bear out the conclusions drawn and the statements made above.

BIPOLAR ROTATING ARMATURE ALTERNATORS.

In column *D* of the specifications on pages 133 to 137, there is set forth a rotating armature design for 3000 R.P.M. and 2 poles, as an alternative to the rotating field design in column *C*. These two designs will constitute a basis for investigation of the applicability of rotating armature designs to bipolar alternators.

We have seen that the principal difficulty in connection with the 2-pole design is the cramping of the field winding space. On page 135, it may be noted that whereas in the 4-pole design the armature copper weighs 0.16 ton and the field copper 0.32 ton, in the 2-pole design the armature copper weighs 0.30 ton and the field copper only 0.22 ton. Since the field ampere turns are several times the armature ampere turns, it would seem rational to use as the armature the rotating element on which the winding space is very restricted. Such designs are outlined at the right in Fig. 78, there being shown two types of frame, the one circular and the other rectangular. There is ample room for the armature copper and the current density is only 250 amperes per square centimetre.

A further advantage realised in the rotating armature design, is that the lengths of the magnetic paths in the poles and yoke are much greater, and a higher degree of saturation is obtained. In design C the iron accounts for only 4 per cent of the total field ampere turns, while in design D the iron accounts for 10 per cent, as will be seen from the saturation curves in Figs. 91 and 92. Thus for the same

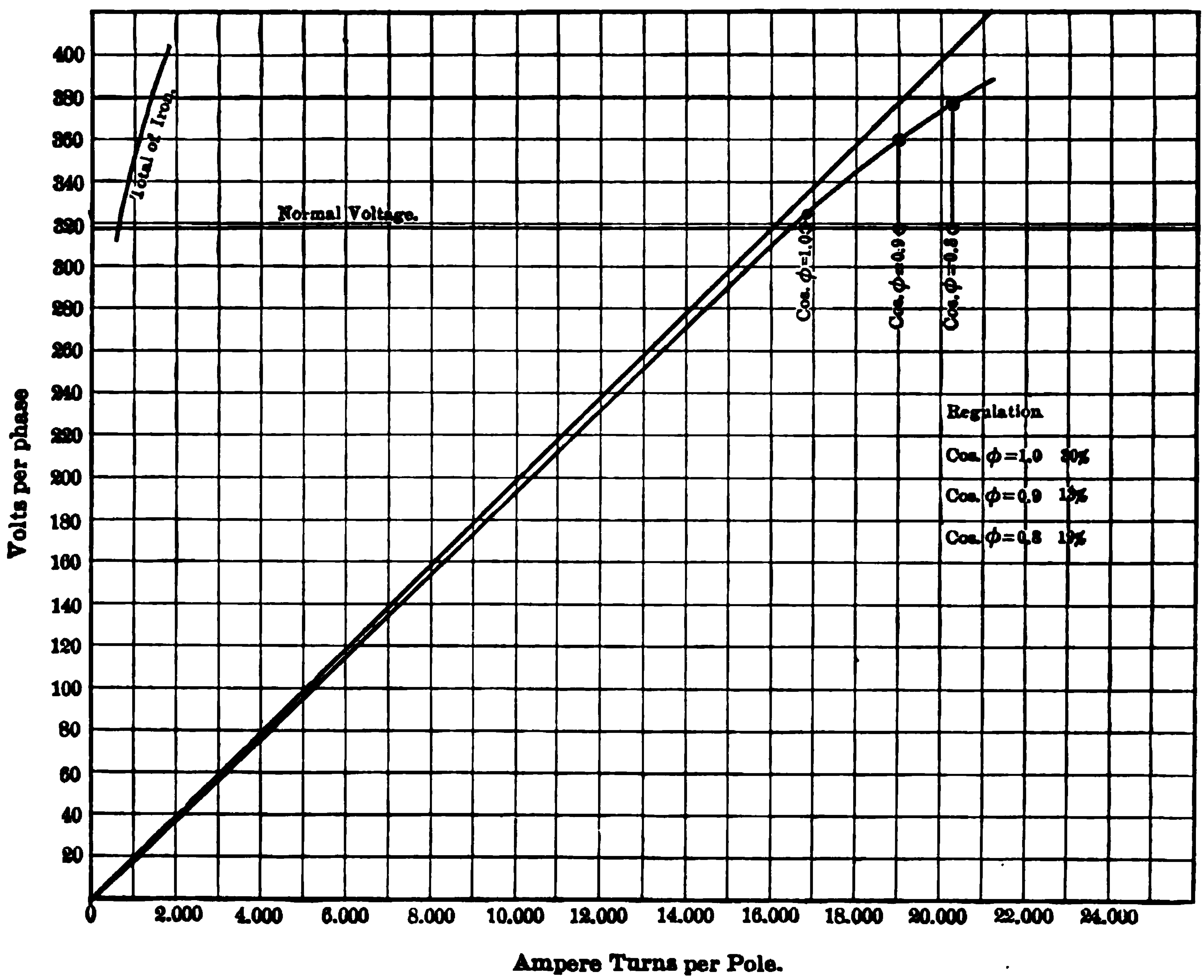


Fig. 91.— Saturation curve for 400 kva. 3-phase 2-pole 3000 r.p.m. 50 cycle 550 volt alternator (rotating field).

regulation, a considerably lower ratio of field to armature ampere turns may be employed in design D. As the field coils in design D are stationary, they must be more liberally proportioned on account of heating. Hence the weight of the field copper in design D is 0.43 ton against 0.19 in design C. The total weight of copper in D is, however, only 0.62 ton against 0.52 in C. This comparatively small

increase in outlay for copper greatly facilitates the design problem, especially the question of heating of the field copper.

The other chief advantage of the rotating armature design lies in the large reduction in weight of armature laminations and consequently armature core loss and heating. The weights of armature laminations are 0.64 ton for design D, and 2.42 tons for design C.

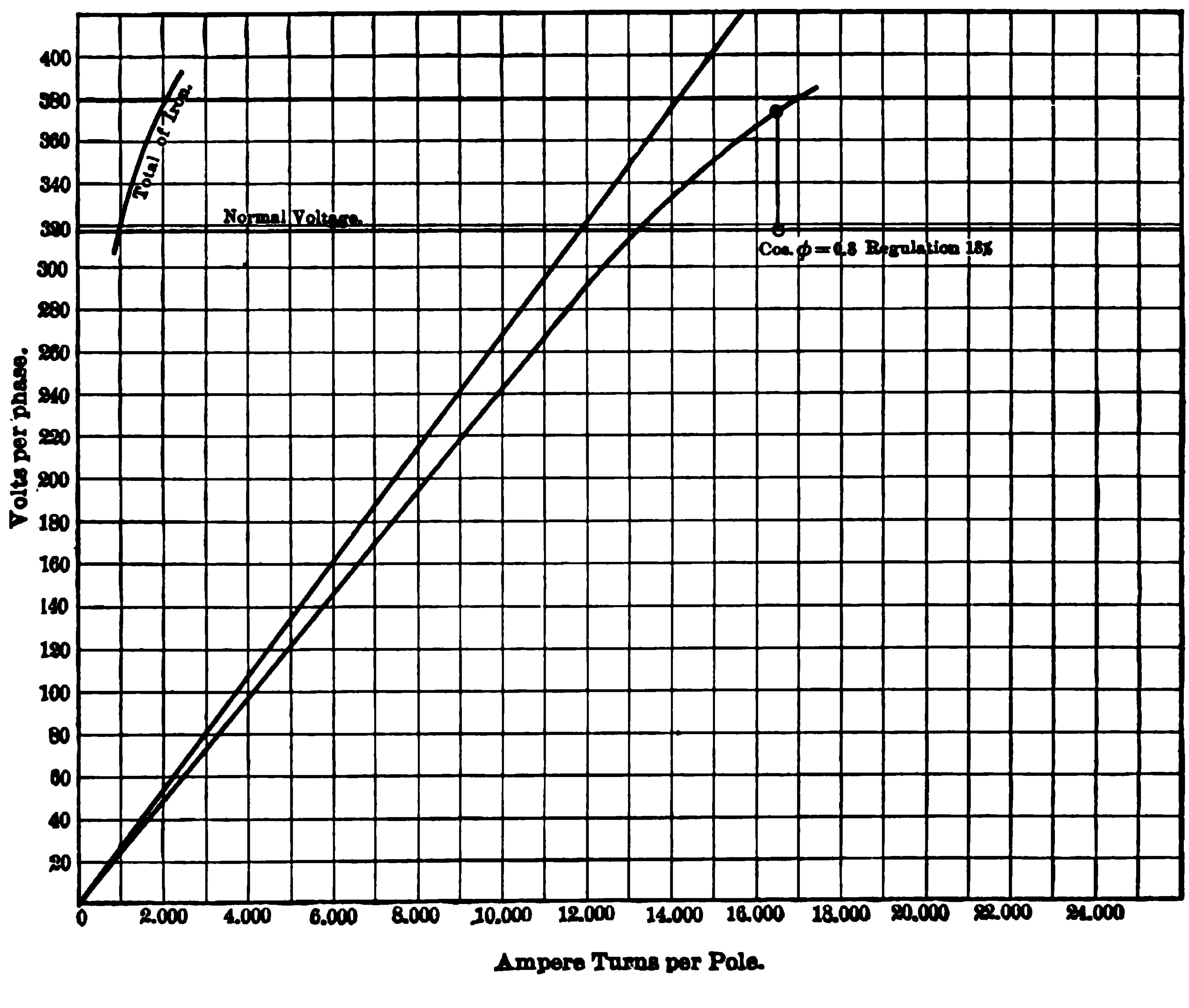


FIG. 92.— Saturation curve for 400-kva. 3-phase 2-pole 3000 r.p.m. 50 cycle 550 volt alternator (rotating armature).

The total core loss is 10.5 kw. for design D against 18.5 for C, and the armature heating 107 watts per square decimetre for design D, as against 156 watts per square decimetre for design C.

A larger amount of material is of course required in the field system, so that the total weight and cost of effective material come out rather greater for the rotating armature design. This is offset by

its better electrical quality, and further by the fact that the total weight is considerably less, as no stator frame is required in the case of the rotating armature design.

The weight factor for design *D* is taken as 1.25, and for design *C* 1.75, giving total net weights of 4.6 tons and 6.5 tons respectively.

Further comparisons between the rotating armature and rotating field bipolar designs may be drawn from the curves in Figs. 80–86, in which the points corresponding to design *D* have been indicated thus ⊕ and marked *A*.

CHAPTER IX.

STUDY OF THE INFLUENCE OF SPEED, NUMBER OF POLES AND FREQUENCY, ON OUTLINE DESIGNS FOR 3000 KVA. ALTERNATORS FOR VARIOUS SPEEDS.

IN order to obtain a fair idea as to the effect of the rated speed on the characteristics of fairly large alternating current generators, as regards design and quality, we have in this chapter compared the designs for a 3000 kva. 3-phase 25-cycle 11,000-volt alternator for speeds ranging from 750 R.P.M. with 4 poles down to 83 R.P.M. with 36 poles. Further, we have taken designs for the same rating, but for a frequency of 50 cycles and for speeds of 750, 1000, and 1500 R.P.M. with 8, 6, and 4 poles respectively. These designs tend to show that the cost and quality of the design do not depend so much on the actual rated speed as on the number of poles associated with the speed, i.e., on the frequency associated with the speed. We are led in fact to conclusions similar to those arrived at in the previous chapter.

In Chapter X we shall study a 6000 kva. rating, and shall develop comparative designs for a constant speed of 750 R.P.M., but with frequencies of 25, 37.5, and 50 cycles corresponding to 4, 6, 8 poles. These designs will also show that the more important factor is the number of poles, and this is again observed in the case of the 400 kva. designs in Chapter VIII.

Each of these groups will ultimately be discussed. At this point we are concerned with the influence of the single element of speed for a given rated output and frequency. On pages 168 to 170 are given specifications for 3000 kva. 25 cycle 11,000 volt 3-phase alternators for the following speeds and number of poles:

Number of poles	4	6	8	12	24	36
Speed R.P.M.	750	500	375	250	125	83

This range of speeds covers the practicable range for a rating of 3000 kva. The next lowest probable speed may be taken as

75 R.P.M., but a design at such a speed has an abnormally large diameter and is in almost all respects an undesirable design. The next highest possible speed is 1500 R.P.M. with 2 poles. A design at this speed should not be called for, as a turbine for 3000 kva. should preferably not be built to run at a speed higher than 1000 R.P.M. Such a design would also be very difficult and inferior, as we shall see at a later point where we have roughly determined the principal features of the design. It is also seen from Chapter VIII, that 2-pole alternators are beyond the limits of *good* designs.

Before proceeding with a critical comparison of the six alternators, we shall indicate the method of procedure adopted in developing the designs. This is practically identical with that outlined in Chapter VII, but the procedure in obtaining preliminary designs for the whole group simultaneously is interesting. In developing such a group of designs, the principal dimensions and quantities may be simultaneously determined for all the designs and carried along in parallel columns. This has been done so far as the preliminary designs are concerned, but, after a certain point at which refinements in each design become necessary, each has been taken individually and the best design for the particular case has been obtained. Thus the designs are worked out with reference to one another only in the early stages; and before arriving at the final designs, so much independent work has been done on each without reference to the others, that the ultimate results may be taken as constituting a very fair comparison.

Main Dimensions.

Following the procedure given in Chapter VII, a value for the output coefficient ξ is first chosen from the curves in Fig. 3. The values of ξ corresponding to the final designs will be found on page 170 and these are not widely different from the values indicated in the curves, although they have been adjusted to correspond to the revised designs.

The gap diameter D is assigned a preliminary value from the curves in Fig. 69. The actual values finally employed are shown on page 168, and these again are fairly close to the values originally obtained from the curves for purposes of the preliminary stages of the design. Having now values for ξ and D, we may at once obtain the values for the armature gross core length λ_g. The

ratios $\frac{\lambda_g}{\tau}$ and $\frac{D}{\lambda_g}$ are now tabulated, and an inspection of these will reveal any radically faulty assumptions in the dimensions, and if necessary the values of D and λ_g may be readjusted. The ratio $\frac{\lambda_g}{\tau}$ is entered on p. 170, and it will be seen that its value varies from 1.25 in the 36-pole (83 R.P.M.) design to 2.1 in the 4-pole (750 R.P.M.) design. It is not practicable in a machine of this rating to reduce $\frac{\lambda_g}{\tau}$ to a still smaller value in order to give a better shape to the magnet core section, as this would lead to excessively large diameters, entailing in the high speed machines, impracticably high peripheral speeds, and in the low speed machines, an excessive amount of structural material for the frame and fly-wheel, and as a consequence high Total Works Cost.

Armature Turns and Flux. — Having now determined the main dimensions D, λ_g and τ, we next require to settle relative values for the armature turns and the flux. We may either assume values for α, the ampere conductors per centimetre of armature periphery, or for β, the flux per square centimetre of total air gap surface. For this group of designs we may for a preliminary value take $\beta = 5000$, and so adjust the actual values obtained for the flux per pole, as to suit the nearest whole number of turns per phase. It would be more correct to take lower values for β in the machines having lower output coefficients, but as the range of values for ξ is not large, a uniform value for β may be assumed, as this will enable us to determine the flux per pole for all the designs simultaneously.

The flux per pole in megalines is $M = \beta\tau\lambda_g \times 10^{-6}$. If $\beta = 5000$, $M = 0.005\ \tau\lambda_g$ megalines. This gives the following values for M for the six designs taken in order: 77, 60, 44, 30, 17, 12.

The number of turns per phase, T, now follows from the relation $V = 4.44\ TNM \times 10^{-2}$.

We have $V = 6350$ volts per phase and $N = 25$,

$$\therefore\ T = \frac{6350 \times 100}{4.44 \times 25 \times M} = \frac{5700}{M}.$$

This gives the following preliminary values for T: 74, 95, 130, 190, 336, 474. The precise number of turns must be so chosen as to give an even number of conductors per slot.

Number of Slots and Conductors per Slot.—The number of slots per pole per phase is shown in Table 29, and ranges from 5 in the 4-pole design to 2 in the 36-pole design. These values correspond to an average slot pitch of about 6.5 centimetres, and a total number of slots ranging from 60 to 216.

In this table there are also shown the number of conductors per slot leading to the numbers of turns per phase corresponding most nearly to the values obtained above from the assumed value of β. The values for T are very close to those originally obtained, and we are now able to correct the values of the flux M in accordance with the actual number of turns per phase. This has been done, and the corrected values for the flux are entered up in Table 29.

TABLE 29.

SHOWING NUMBER OF SLOTS, CONDUCTORS PER SLOT, TURNS AND FLUX.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of poles	4	6	8	12	24	36
Slots per pole per phase	5	4	4	3	2	2
Slots per pole	15	12	12	9	6	6
Total number of slots	60	72	96	108	144	216
Slot pitch	6.28	6.6	5.9	6.67	8.3	7.3
Slots per phase	20	24	32	36	48	72
Conductors per slot	7	8	8	10	13	13
Conductors per phase	140	192	256	360	624	936
Turns per phase	70	96	128	180	312	468
Revised value for flux M	81	59	45	32	18.7	12.5

Size of Magnet Cores. — The dimensions of the magnet cores are determined from the flux in the magnet cores and the suitable density.

The leakage coefficients assumed, range from 1.20 in the 4-pole design to 1.35 in the 36-pole design. These values are afterwards checked from the dimensions of the magnet cores and the excitation on each pole. The method is described on page 76, Chapter V.

If on checking the values by this method, a wide divergence is found, the flux in, and the size of, the magnet core must be readjusted. In all these machines, laminated magnet cores are taken, and a flux density in the pole of 14,500 lines per square centimetre has been taken throughout. The magnetic cross section of the core is given by the flux divided by the density. Dividing the cross section by the net axial length of the magnet core parallel to the shaft, the breadth c, normal to the shaft is obtained.

These steps are set forth in Table 30.

TABLE 30.

DETERMINATION OF DIMENSIONS OF MAGNET CORE.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of poles	4	6	8	12	24	36
Leakage coefficient	1.2	1.2	1.2	1.25	1.3	1.35
Flux in magnet core	97	71	54	40	24	17
Magnetic section	6700	4900	3700	2150	1650	1170
Net axial length of magnet core	198	147	118	95	65	52
Breadth c of magnet core	34	33.5	31.5	29	25	21.5

Magnet Yoke. — The magnetic density which may be permitted in the yoke depends on the material employed. This in turn is dependent on the type of construction, which again is largely controlled by the number of poles and the speed.

For the 4-pole design we may have a forged solid steel hub with the laminated poles dovetailed into planed longitudinal recesses.

For the 6-pole design a laminated hub has been taken, as this will permit of providing ventilating tunnels in the stampings at the corners of the hub (see the 6-pole rotor illustrated on pages 264 and 265).

The 8-pole machine has a laminated steel yoke with the pole and portion of the yoke in one stamping similar to the structure illustrated on page 275, Chapter XI.

The 12-pole machine might also be built on this plan, but for this machine and for the 24- and 36-pole designs a cast iron yoke is shown. The dimensions of the yoke section are determined in the same way as those of the magnet cores, and are set forth in Table 31.

TABLE 31.

DETERMINATION OF DIMENSIONS OF YOKE.

Material of Yoke.	Wrought Steel.	Laminated Steel.	Laminated Steel.	Cast Iron.	Cast Iron.	Cast Iron.
Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of poles	4	6	8	12	24	36
Flux density	...	...	14,500	6000	6000	6000
Magnetic cross section	...	...	1,860	3330	1950	1350
Length axially (effective)	198	140	118	115	85	70
Radial depth	18	30	16	29	23	19.5

For the 8-, 12-, 24-, and 36-pole designs, the cross section of the yoke may be determined from the appropriate density. For the 4- and 6-pole designs, however, the yoke section extends down to the shaft,

and the radial depth entered in the table is obtained from outline sketches of the machine (see Figs. 93 and 94). In these two cases the yoke section is too indefinite to permit of calculation and may be ignored in the magnetic calculations.

The diameter of the yoke is determined by the radial length of the magnet cores. This is fixed by the space required by the field copper, which should be estimated at this point, as the question of whether or not to proceed with the design as evolved up to this stage, depends on whether the requisite amount of field copper can be got into the available space.

Field Copper. — The approximate number of ampere turns required on each field pole may be determined from the armature strength with a degree of accuracy sufficient for the present preliminary purpose. The determination of the *precise* excitation required, involves a detailed estimation of armature reactions in the manner given in Chapter V. This process is rather laborious to carry out for each of these six designs, and as our main purpose is a comparison of the general characteristics of the designs the following preliminary determinations will suffice.

For a machine to come within reasonable limits as to pressure regulation, the short circuit current ranges from 2 to 3 times the full load current of the machine. This has already been discussed at page 115 of Chapter VII.

The number of field ampere turns per pole must be from 1.5 to 2 times the number of armature ampere turns per pole, i.e., it must be from 1.5 to 2 times the "armature strength." The value of this ratio depends on the degree of saturation of the machine, i.e., on the proportion which the ampere turns for the iron parts of the magnetic circuit bear to the ampere turns for the air gap — or on the ratio of air gap ampere turns to total ampere turns. With a highly saturated machine, the ratio of field ampere turns to armature strength may be lower, and vice versa, as will be seen on reference to the investigation on pages 81 to 86 of Chapter V.

It is a characteristic of high speed machines with few poles, that the magnetic circuit is cramped, the lengths of the flux paths in the iron being small owing to the small dimensions. For this reason the bulk of the ampere turns on the field pole have to be expended on the air gap which leads to still longer gaps than would be necessary if more magnetomotive force could be expended on the iron.

We shall not make detailed calculations for the magnetic circuits of these six designs. It will be sufficient to assume values for the proportion of the air gap to total ampere turns, in the light of the above considerations. With these considerations in mind we shall take appropriate values for the ratio of field ampere turns to armature ampere turns. The following are suitable values for these two quantities for the six designs:

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of poles	4	6	8	12	24	36
Ampere turns for air gap in per cent of total field ampere turns per pole	95	92	89	86	83	80
Ratio of field to armature ampere turns	2.0	1.75	1.75	1.5	1.5	1.5

Multiplying the armature strength by the above ratios gives the field ampere turns per pole at no load. The maximum ampere turns that the magnet core will have to carry will correspond to the excitation for full load at a power factor equal to 0.8. To obtain this, the no load excitation must be increased by the amount of the excitation regulation which is the percentage increase in excitation from no load to full load at a given power factor.

The excitation regulation depends again on the degree of saturation as noted in Chapter V. It is greater the higher the degree of saturation, and hence for machines where the larger proportion of the ampere turns are expended on the air gap, the excitation regulation is lower. Hence of the six designs under consideration those with few poles should, strictly speaking, have lower excitation regulation. As, however, the values will not differ widely, we may, for all the six designs, take the excitation regulation as 40 per cent at a power factor of 0.8, and this figure will afford a conservative basis for the estimation of the field copper.

Thus, in each case, to obtain the maximum excitation required on the magnet core, we multiply the no load excitation by 1.4. In Table 32 the first line gives the values of the armature strength; the second line the ratio of field to armature ampere turns chosen above; the third line the no load field excitation which is the product of the values in the first and second lines. The fourth line is the maximum field excitation (for full load at $\cos \phi = 0.8$), and is obtained by multiplying the no load excitation in the third line by 1.4.

TABLE 32.

ESTIMATION OF FULL LOAD FIELD EXCITATION.

Armature strength in ampere turns per pole (ni)	8,250	7,500	7,500	7,060	6,350	6,350
Ratio of field to armature ampere turns (R)	2.0	1.75	1.75	1.5	1.5	1.5
No load field ampere turns $=(R\times ni)$	16,500	13,100	13,100	10,600	9,500	9,500
Maximum required field excitation (*i.e.*, the field excitation for a power factor of 0.8)($=1.4\times R\times ni$)	23,000	18,300	18,300	14,900	13,300	13,300
Speed	**750**	**500**	**375**	**250**	**175**	**83**

The space required by these numbers of ampere turns is determined from the heating considerations. The following formula affords a convenient starting point for field spool calculations:—

$$\text{C.D.} = \frac{W}{\text{A.T.}l\rho}.$$

Where W = watts lost in coil.

C.D. = current density in amperes per square centimetre.

A.T. = ampere turns on spool.

l = mean length of turn in centimetres.

ρ = specific resistance of copper (= 0.0000020 ohms per centimetre cube at 60° C).

The total copper section in square centimetres is $\frac{\text{A.T.}}{\text{C.D.}}$, so that a value for the current density determines the space required by the field copper. In the above expression for C.D., the values of A.T. and ρ are given, and an approximate value for mean length of turn l may be taken for each design, as its value is not greatly affected by the winding depth. The only other factor is W, the watts lost. This is determined from the permissible watts per square decimetre of external spool surface. It is sufficient at this stage to set out an outline drawing of each machine on similar lines to the drawings in Figs. 93 and 94, and scale the dimensions of the space available for the field winding, from which the current density, watts, and heating may be easily calculated.

These steps are set forth in Table 33:

TABLE 33.

CALCULATION OF FIELD WINDING.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of poles	4	6	8	12	24	36
Length of winding space . . . . a	18	20	20	20	20	20
Depth of winding space . . . . b	10	7.0	7.5	6.5	6.0	6.0
Cross section winding space $a \times b = c$	180	140	150	130	120	120
Space factor assumed . . . . . . d	0.7	0.7	0.7	0.65	0.65	0.65
Total copper section $c \times d =$ . . . e	126	98	105	85	78	78
Ampere turns . . . . A.T.	23,000	18,300	18,300	14,900	13,300	13,300
Current density C.D. $= \frac{\text{A.T.}}{e}$	183	187	174	177	170	170
Internal dimensions of spool. . . .	198 × 34	155 × 33.5	124 × 31.5	100 × 29	68 × 25	55 × 21.5
Mean length of turn l	512	408	345	298	213	179
Watts = A.T. × C.D. × l × ρ . . .	4300	2800	2200	1570	970	810
External periphery of spool	556	435	378	318	238	200
External surface of spool sq. dcm. .	100	87	75	63	47.5	40
Watts per sq. dcm..	43	32	29	25	20	20
Peripheral speed of field, in metres per second	47	39	35	30	25	22

In the above table there are first given the dimensions and section of the winding space. It is not necessary to carry out detailed calculations of the size of conductor required for the field, but so long as the total copper section is obtained, this is sufficient. Hence an appropriate value for the space factor has been assumed for each case, and the copper section has been obtained by multiplying the total winding section by the value of the space factor. The mean length of turn is obtained from the internal dimensions of the spool (which are the dimensions of the magnet core obtained) and from the winding depth. The watts per spool may next be calculated and the external surface of the spool is obtained from its dimensions. In this way the watts per square decimetre of surface is arrived at. This value constitutes the criterion of whether the current density is or is not too high, i.e., whether there is sufficient room for the requisite amount of copper.

It will be seen from the table that the value of the watts per square decimetre ranges from 47 in the 750 R.P.M. design to 22 in the 83 R.P.M. design. At the foot of the table the rotor peripheral speed is entered, as this should be kept in mind in interpreting such a heating coefficient as the watts per square decimetre. The values of the watts per square decimetre obtained above are all of suitable amounts, when associated with correct design in other respects, so that the field copper may be allowed to stand as thus determined and the weight may be estimated.

If the heating had come out too high in any one case, it would have been necessary to increase the length of winding space, i.e., the radial length of the magnet pole, in order to increase the total winding space; or else to increase λg and so make the pole core narrower and obtain a greater winding depth. If sufficient room cannot be obtained in this way, the design must be revised and a weaker field and correspondingly weaker armature employed.

We have now seen that there is room on the poles for the required amount of field copper for the designs having the present values for armature strength. Consequently we are justified in proceeding with further calculations on the armature with a view to completing the designs.

Armature Slots and Teeth. — We have already decided on the number of slots for each case and we have now to determine the dimensions of the teeth and slots. As flux density in the teeth, we have taken 18,000 lines per square centimetre throughout. Dividing the flux per pole by the density, gives the magnetic cross section of the teeth under one pole which may be denoted by A_t.

The number of teeth directly opposite the pole face is 0.7 × number of slots per pole. Allowing 10 per cent for spreading, the number of teeth carrying the flux, or the teeth within the effective pole arc, is 1.1 × 0.7 = 0.77 × number of slots per pole; denoting this number by q, we have for t, the width of the tooth, the expression: —

$$t = \frac{A_t}{q\lambda n}.$$

Where λn = armature net core length.

These calculations are shown in Table 34.

TABLE 34.

DETERMINATION OF SLOT DIMENSIONS.

Speed	**450**	**500**	**375**	**250**	**125**	**83**
Number of Poles	4	6	8	12	24	36
Flux entering armature per pole . M	81	59	45	32	18	12
Magnetic cross section of teeth . A_t	4500	3300	2500	1800	1040	700
Number of teeth per pole	15	12	12	9	6	6
Number of teeth in effective pole arc q	11.5	9	9	7	4.6	4.6
Armature net core length . λ_n	138	108	87	70	47.5	38.5
Width of tooth $\frac{A_t}{q \times n} = t$	2.85	3.4	3.2	3.7	4.75	3.9
Slot pitch . τ_s	6.28	6.6	5.9	6.67	8.3	7.3
Slot width . S	3.43	3.2	2.7	3.0	3.65	4.0

For machines of this output and voltage, the depth of slot is largely determined by the size of the armature conductors. This is fixed by the value of the current density. In any single design the slot may be worked out from the preliminary assumption of a current density of, say, 300 amperes per square centimetre, and a slot of good proportions obtained by adjusting the current density. In the group of six designs, the relative values of the current density in each case should be determined by consideration of the distribution of the armature losses. In the high speed few-pole designs the iron loss is some 5 to 10 times the copper loss, while in low speed many-pole machines it is only from 1 to 2 times the copper loss. In the low speed machines the losses are thus unavoidably more concentrated at the active belt, while in the high speed machines the loss in the body of the armature iron largely preponderates. On this account it will be consistent to adopt higher current densities and lower flux densities in the high speed machines than in the low speed machines in order to obtain a more uniform distribution of losses, and a flatter efficiency curve with a view to maintaining higher efficiency at light loads.

The values of the current densities finally taken, are given in Table 35 which also sets forth the calculation of the size of conductor and the depth of slot. The latter is arrived at by assuming an appropriate value for the space factor and avoiding detailed calculations.

TABLE 35.

DETERMINATION OF SIZE OF CONDUCTORS.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of Poles	4	6	8	12	24	36
Current density	280	260	240	220	210	210
Cross section of conductor	0.56	0.60	0.65	0.71	0.75	0.75
Number of conductors in slot	7	8	8	10	13	13
Total copper section	3.9	4.8	5.2	7.1	9.75	9.75
Space factor	0.25	0.26	0.28	0.3	0.33	0.33
Total area of slot	15.6	18.75	18.75	23.4	29.5	29.5
Depth of slot	4.6	5.5	7.0	7.8	8.0	8.6

Depth of Armature Iron Below Slots.— This is determined from the flux density and corresponding iron losses and heating. We have in Table 36 taken appropriate values for the flux density and calculated the radial depth and external diameter of the armature laminations from the flux.

TABLE 36.

DETERMINATION OF EXTERNAL DIAMETER OF ARMATURE CORE.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of Poles	4	6	8	12	24	36
Flux in armature M	31	59	45	32	18	10
Flux density β_a	9,000	9,500	10,000	10,000	10,000	10,000
Magnetic cross section A_a	9,000	6,200	4,500	3,200	1,800	1,202
Radial depth below slots $\frac{A_a}{2\lambda_n} = h$	32.5	29	26	23	19	15.5
External diameter	196	217	246	291	434	548

Whether these dimensions and densities may be adhered to, depends chiefly on the heating coefficient of the armature corresponding to the losses, which we shall now calculate.

Losses, Heating, and Efficiency. — The data and calculations for the copper and iron losses are set out in Table 37, parts (a), (b) and (c).

TABLE 37.

CALCULATION OF LOSSES AND HEATING.

(a) COPPER LOSS.

Speed	**750**	**500**	**375**	**250**	**125**	**83**
Number of Poles	4	6	8	12	24	36
Mean length of turn ($= 2\lambda g + 4\tau$ cms.)	770	620	530	440	336	285
Length of conductor per phase m.	54	59.5	68	79	110	138
Resistance per phase ohms	0.193	0.200	0.205	0.220	0.300	0.374
I^2R loss for 3 phases kw.	14.1	14.5	15.3	16.5	22.0	27.7

(b) IRON LOSS IN ARMATURE BODY.

Flux density β_a	9.0	9.5	10.0	10.0	10.0	10.0
Watts per kg. (see Fig. 75)	3.24	3.6	4.0	4.0	4.0	4.0
Weight of armature iron below slots — tons	18	14.3	12.1	10.6	9.1	7.8
Core loss kw.	57.5	51.5	48.5	42.5	36.4	31.2

(c) IRON LOSS IN TEETH.

Flux density β_t	18	18	18	18	18	13
Watts per kg. (see Fig. 75)	13	13	13	13	13	18
Weight of teeth — tons	0.97	1.07	1.43	1.68	1.92	2.07
Tooth loss kw.	11	14	18.6	22	25	27
Total armature losses kw. ($=a+b+c$)	82.5	80.0	82.4	81.0	83.4	85.9
Heating coefficient						
(1) Surface $\pi D(\lambda g + 0.7\tau)$	1000	1020	980	1020	1230	1330
Watts per sq. dcm.	83	78	84	80	68	65
(2) Surface $\pi D\lambda g$	750	730	700	720	810	865
Watts per sq. dcm.	100	100	106	100	86	79

The above values for the heating coefficient will, with careful design, not result in excessive heating, and the designs are now fairly complete. If the heating had come out excessive, the designs would need revising, using larger dimensions or lower densities.

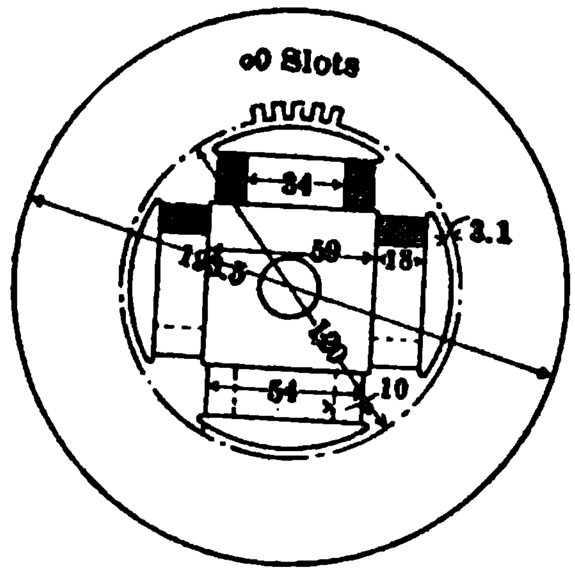
The complete designs are set forth in the following specifications, to which are appended an analysis of weights and costs of the material and data of the calculated values for the leading coefficients and constants.

Outline drawings of the machines are given in Figs. 93 and 94.

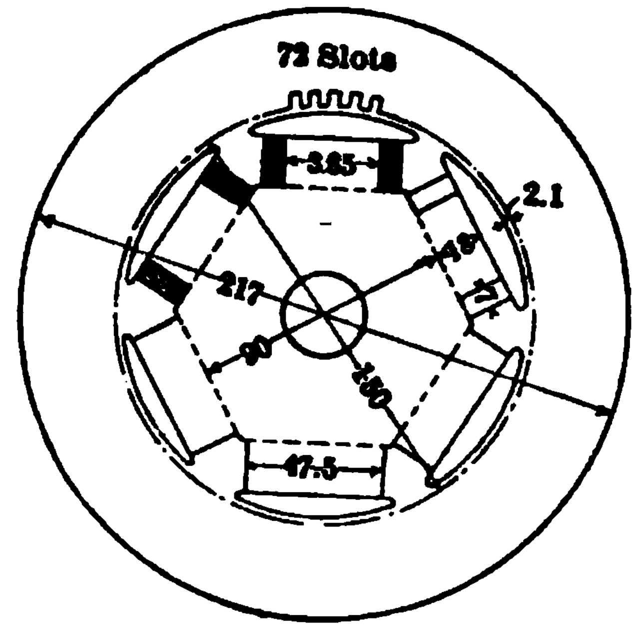
SPECIFICATION FOR
3000 Kva. 3-Phase 25 Cycle 11,000 Volt
ALTERNATING CURRENT GENERATORS.

All Dimensions in Cms.

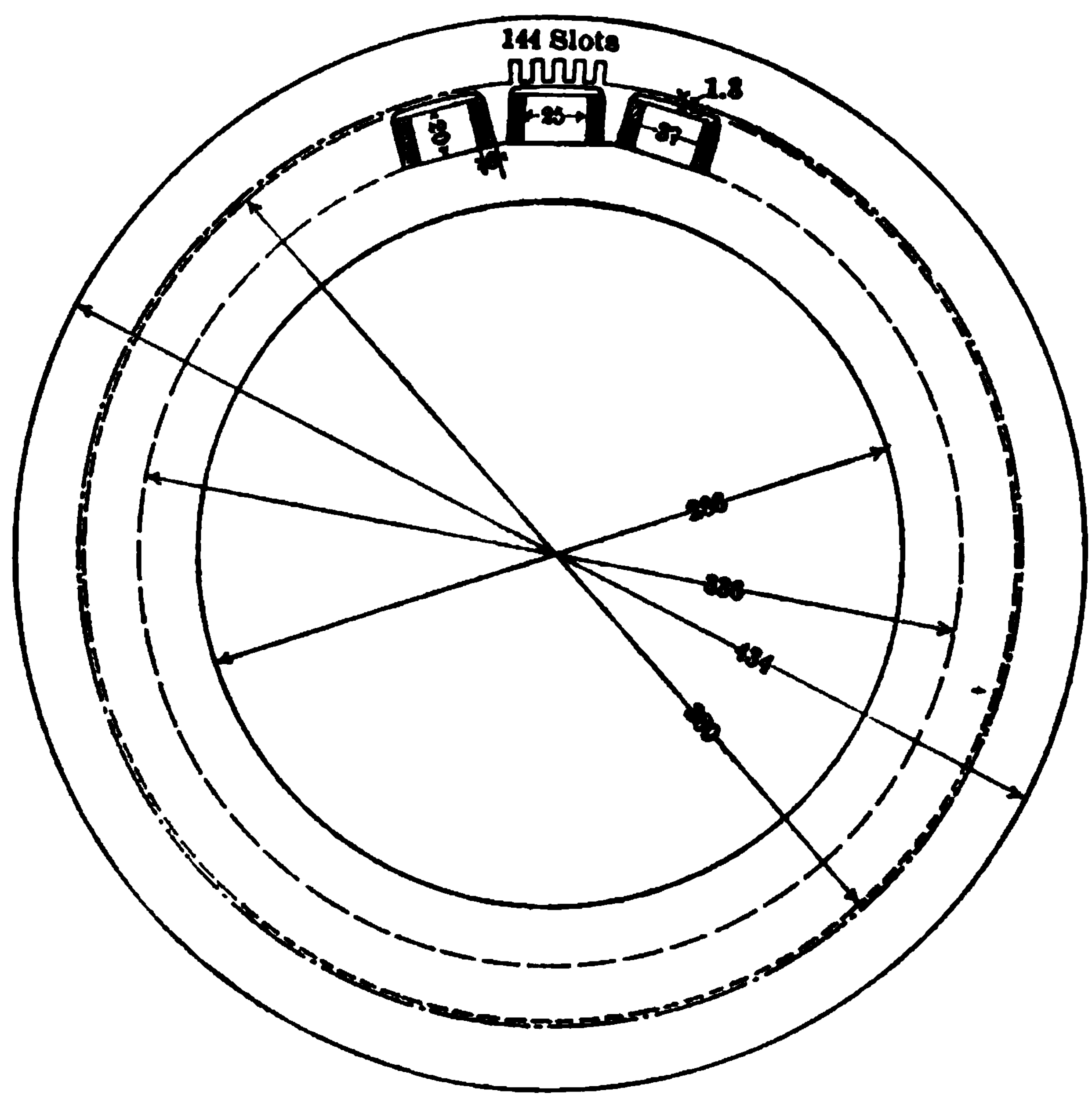
	A.	B.	C.	D.	E.	F.
Output in kva.	3,000	...	...	...	...	...
Terminal voltage	11,000	...	...	...	...	...
Style of connection	Y	...	...	...	...	...
Current per terminal	157	...	...	...	...	...
Speed R.P.M.	**750**	**500**	**375**	**250**	**125**	**83**
Frequency	25	....	...	...	...	...
Number of Poles	**4**	**6**	**8**	**12**	**24**	**36**
ARMATURE IRON						
Diameter at air gap D	120	150	180	230	380	500
Diameter at bottom of slot	130.5	160	194	205.6	396	517
External diameter of laminations	196	217	246	291	434	548
Depth above slots h	32.5	29	26	23	19	15.5
Gross length between coreheads λg	198	155	124	100	68	55
Number of ventilating ducts	29	21	18	15	10	8
Width of each duct	1.5	1.5	1.5	1.5	1.5	1.5
Effective core length (iron) λn	138	108	87	70	47.5	38.5
SLOTS AND TEETH						
Total number of slots	60	72	96	108	144	216
Slot pitch at armature face	6.28	6.6	5.9	6.67	8.3	7.3
Width of slot	3.43	3.2	2.7	3.0	3.65	4.0
Width of tooth at armature face	2.85	3.4	3.2	3.7	4.75	3.9
Radial depth of slot	4.6	5.5	7.0	7.8	8.0	8.6
ARMATURE COPPER						
Number of slots per pole per phase	5	4	4	3	2	2
Number of conductors per slot	7	8	8	10	13	13
Section of conductor	0.55	0.60	0.65	0.71	0.75	0.75
Current density—amperes per sq. cm.	280	260	240	220	210	210
Number of turns in series per phase	70	96	128	180	312	468
Armature strength (ampere turns per pole)	8,250	7,500	7,500	7,060	6,350	6,350



4 Pole 750 Rev.
$D = 120$, $\lambda g = 198$.

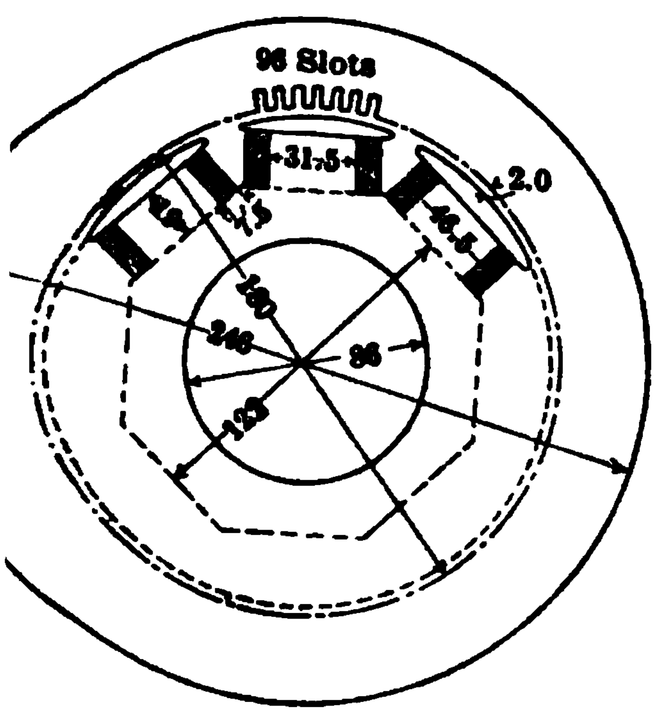


6 Pole 500 Rev.
$D = 150$, $\lambda g = 155$.

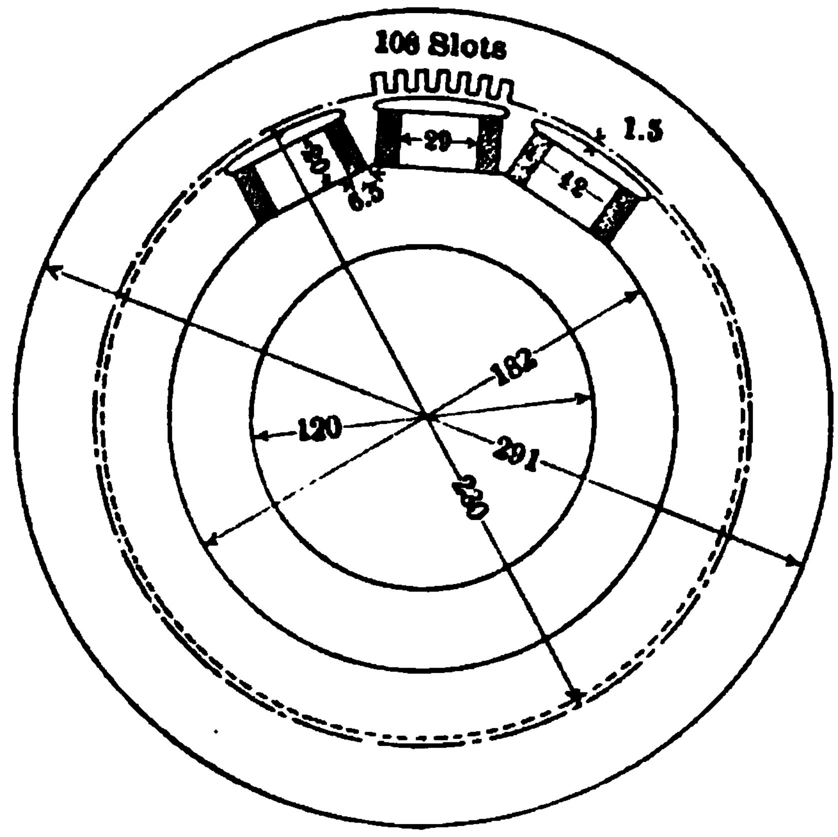


24 Pole 125 Rev. $D = 380$, $\lambda g = 68$.

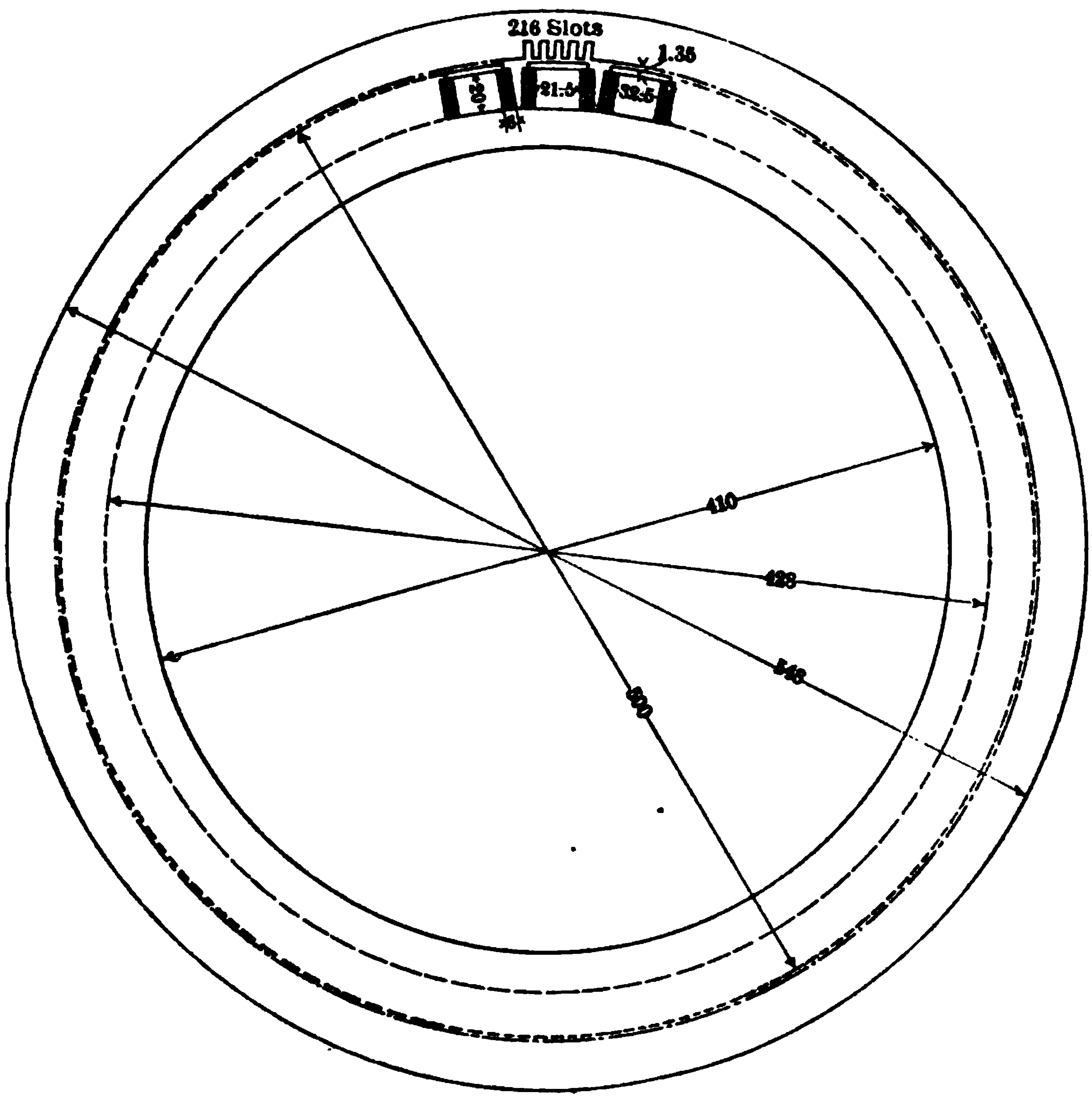
FIG. 93—OUTLINE DRAWINGS OF 3000 KVA., 25 CYCLE, 11000 VO
SPEEDS FROM 750 R.P.M. 4 P



8 Pole 375 Rev.
$D = 180, \quad \lambda g = 124.$



12 Pole 250 Rev.
$D = 230, \quad \lambda g = 100.$



36 Pole 83 Rev. $D = 500, \quad \lambda g = 55.$

LT ALTERNATING CURRENT GENERATORS AT VARIOUS RATED POLES TO 83 R.P.M. 36 POLES.

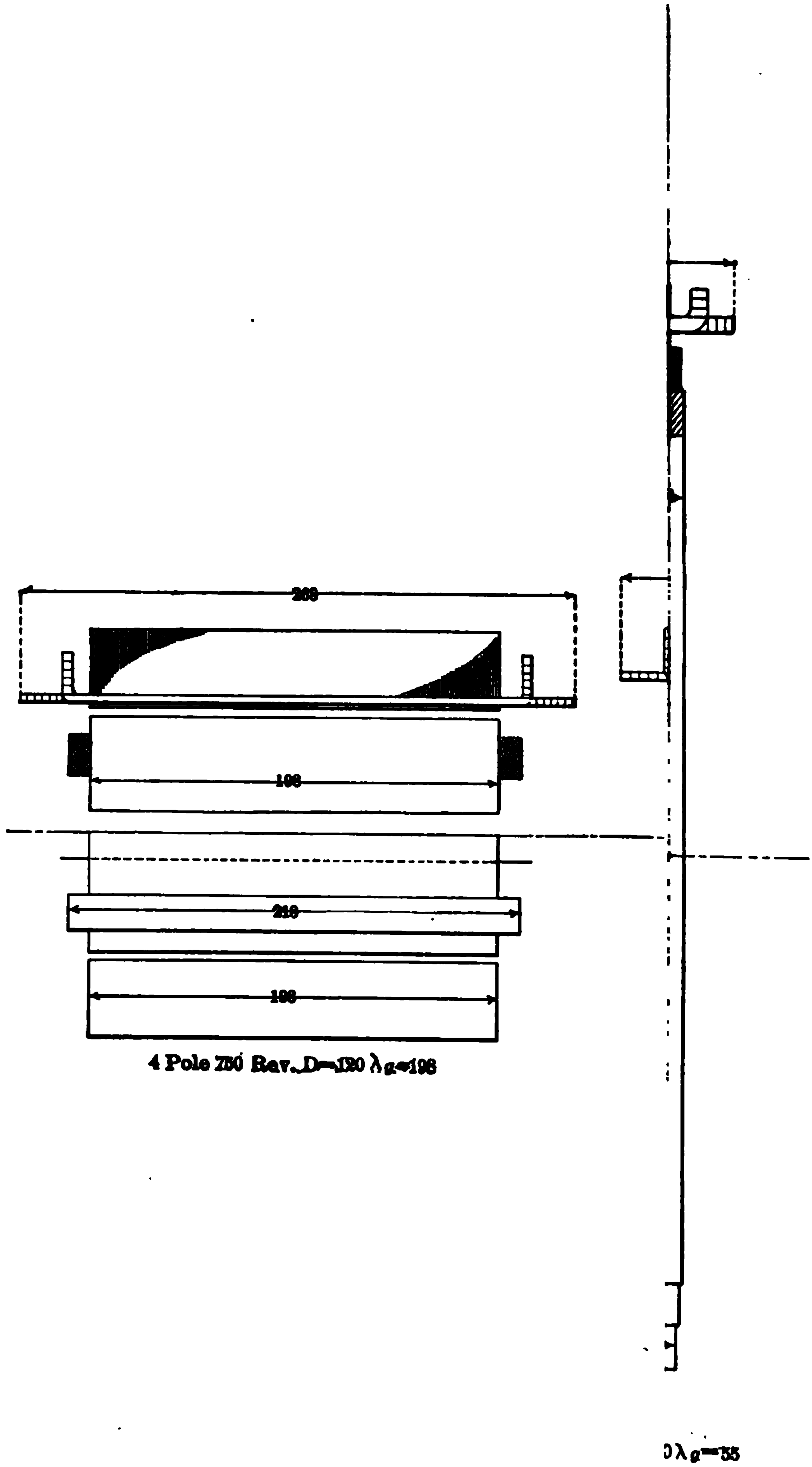


FIG. 94. — OUTLINE DRAWINGS OF 3000 KVA., 25 POLES.

SPECIFICATION FOR 3000 KVA. ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.	E.	F.
ROTOR IRON						
Diameter at pole face	114	146	176	227	377.4	497.3
Depth of air gap δ	3.0	2.0	2.0	1.5	1.3	1.35
Pole pitch at air gap τ	94	78	71	60	50	43.7
Circumferential length of pole arc b	66	55	50	42	35	31
Gross axial length (parallel to shaft)	198	155	124	100	68	55
Effective axial length of pole (iron)	198	147	118	95	65	52
Breadth of pole body across shaft c	34	33.5	31.5	29	25	21.5
FIELD COPPER						
Total length of winding space	18	20	20	20	20	20
Depth of winding space	10	7.0	7.5	6.5	6.0	6.0
Total cross section of winding space per pole	180	140	150	130	120	120
Total cross section of copper	126	98	105	84.5	78	78
Space factor	0.7	0.7	0.7	0.65	0.65	0.65
Ampere turns at full load and $\cos\phi = 0.8$	23,100	18,300	18,300	14,900	13,300	13,300
Weight of copper per spool, kgs.	575	356	323	224	147	124
Current density (amps. per sq. cm.)	183	187	174	177	170	170
MAGNETIC DATA						
Armature flux per pole	81	59	45	32	18	12
Leakage coefficient	1.2	1.2	1.2	1.25	1.3	1.35
Flux in the pole core	97	71	54	40	23.4	16.2
MAGNETIC DENSITIES in kilolines per sq. cm.						
Armature	9.0	9.5	10	10	10	10
Teeth	18	18	18	18	18	18
Pole core — laminations	14.5	14.5	14.5	14.5	14.5	14.5
Yoke	...	...	14.5	6.0	6.0	6.0
Material of yoke	Wro't Steel	Lam. Steel	Lam. Steel	Cast Iron	Cast Iron	Cast Iron
At pole face (air gap)	6.3	7.1	7.3	7.7	7.6	7.2
AMPERE TURNS per pole at no load	16,500	13,100	13,100	10,600	9,500	9,500
Ampere turns for air gap in per cent of total ampere turns	95	92	89	86	83	80
Ratio of field ampere turns to armature ampere turns	2	1.75	1.75	1.5	1.5	1.5
LOSSES IN KW.						
Armature I^2R — Total	14.1	14.5	15.3	16.5	22.0	27.7
Armature Iron — Total	68.5	65.5	67.1	64.5	61.4	58.2
Total armature losses (iron and copper)	82.5	80.0	82.4	81.0	83.4	85.9
Field loss at full kva. ($\cos\phi = 0.8$)	17.2	16.8	17.6	18.9	23.0	29.0
Total losses (excl. friction)	100	97	100	100	106	115
Efficiency (excl. friction losses) — full load	97	97	97	97	96.5	96.5
Efficiency — half load	95	95	95	95	95	95
HEATING						
Watts per sq. dcm. of armature surface reckoned on $\pi D\,(\lambda g + 0.7\tau)$	83	78	84	80	68	65
Do. reckoned on $\pi D \lambda g$	110	100	106	100	86	79
Watts per sq. dcm. of field spool surface at full kva. ($\cos\phi = 0.8$)	43	32	29	25	20	20

SPECIFICATION FOR 3000 KVA. ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.	D.	E.	F.
WEIGHTS OF MATERIAL — Tons						
Magnet poles	5.0	5.35	5.4	6.0	7.0	7.15
Field yoke	6.0	7.45	5.0	12.1	14.6	14.2
Armature laminations	19.0	15.4	13.5	12.3	11.0	10.0
Total effective iron	30.0	28.2	23.9	30.4	32.6	31.4
Armature copper	0.80	0.96	1.18	1.50	2.15	2.72
Field copper	2.30	2.15	2.58	2.70	3.50	4.47
Total copper	3.10	3.11	3.76	4.20	5.65	7.29
Total effective material	33.1	31.3	27.7	34.6	38.3	38.7
COSTS OF MATERIALS . . . $						
Magnet poles	625	670	675	750	875	895
Field yoke	750	930	625	605	730	710
Armature laminations	2,370	1,920	1,690	1,540	1,380	1,230
Total effective iron	3,745	3,520	2,990	2,895	2,985	2,835
Armature copper	500	600	740	940	1,350	1,700
Field copper	1,440	1,350	1,610	1,690	2,180	2,770
Total copper	1,940	1,950	2,350	2,630	3.530	4,470
Total effective material	5,685	5,470	5,340	5,525	6,515	7,305
CONSTANTS AND COEFFICIENTS						
Weight of effective material per kw. kgs.	11.0	10.4	9.2	11.5	12.8	12.9
Cost of effective material per kw.	1.9	1.83	1.78	1.84	2.18	2.44
$D^2\lambda g$ in metres	2.85	3.5	4.0	5.3	9.8	13.7
Ratio $\frac{\lambda g}{\tau}$	2.1	2.0	1.75	1.67	1.36	1.25
Ratio $\frac{D}{\lambda g}$	0.6	0.97	1.45	2.3	5.6	9.1
Output coefficient . . . ξ	1.4	1.7	2.0	2.25	2.05	2.64
Ampere conductors per cm. of periphery . . . a	175	192	210	234	254	290
Flux per sq. cm. of armature surface . . . β	4,350	4,900	5,100	5,300	5,300	5,000
Peripheral speed . . . s	47	39	35	30	25	22
Watts per c. cm. of active belt	8.7	7.5	6.1	5.3	4.6	4.0
Kva. per pole	750	500	375	250	125	83

In order to study the influence of the speed on the design, it will be best to plot against the speed the quantities to be studied. In Fig. 95 the values of $D^2\lambda g$ and the output coefficient ξ are plotted against the speed. The value of $D^2\lambda g$ falls rapidly with increase in speed. The output coefficient does not attain such high values with the high speeds as with the low. This has been noted in Chapter II. If it were possible to obtain a constant value for ξ at all speeds for a given rated output, the value of $D^2\lambda g$ would be exactly inversely proportional to the speed. But as ξ decreases with the speed, the value of

($D^2\lambda g \times$ revolutions per minute) is larger in the high speed designs than in the low.

We shall see that the weight of effective material does not vary nearly so widely, or in the same manner as the value of $D^2\lambda g$. Fig. 96 shows the specific electric and magnetic loadings, α and β, plotted against the speed. The general trend of these is a falling off in value as the speed increases. This accords with the curve for ξ, for it has already been pointed out in Chapter II that ξ is proportional to the

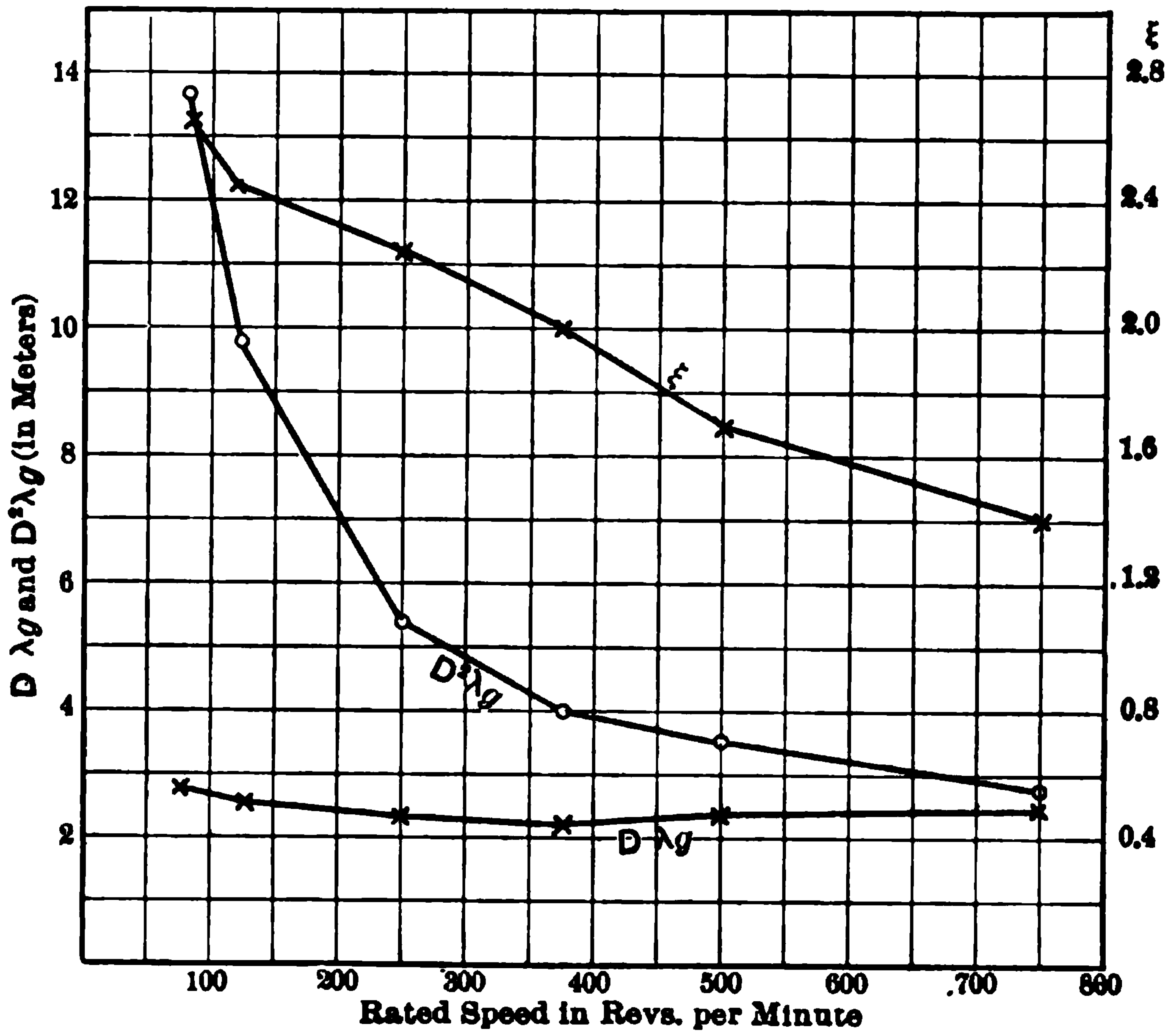


FIG. 95.—$D\lambda g$ and $D^2\lambda g$ as function of rated speed for 3000 kva. 25 cycle designs.

product $\alpha\beta$. One point to observe in this group of designs is that α is relatively higher and β relatively lower at the lower speeds, that is, the low speed machines have relatively somewhat stronger armatures.

This may be ascribed to the fact that the field winding space is generally not so cramped in the low speed multipolar designs. This permits of stronger armatures and correspondingly stronger fields for a given specification as regards pressure regulation.

Fig. 97 gives a set of curves showing an analysis of the losses in each machine. The total electric and magnetic losses are practically

equal for all the designs except for those corresponding to the two lowest speeds.

Hence the efficiency (exclusive of friction losses), is about the same for all cases. It is 97 per cent in the four higher speed designs, and some 96.5 at those for the two lowest speeds, the high speed machines thus showing slightly higher efficiency. At one half load the electrical efficiency is 95 per cent throughout. The distribution of the losses is the chief matter of interest in connection with Fig. 97. The armature iron loss increases and the copper loss decreases with the higher speeds.

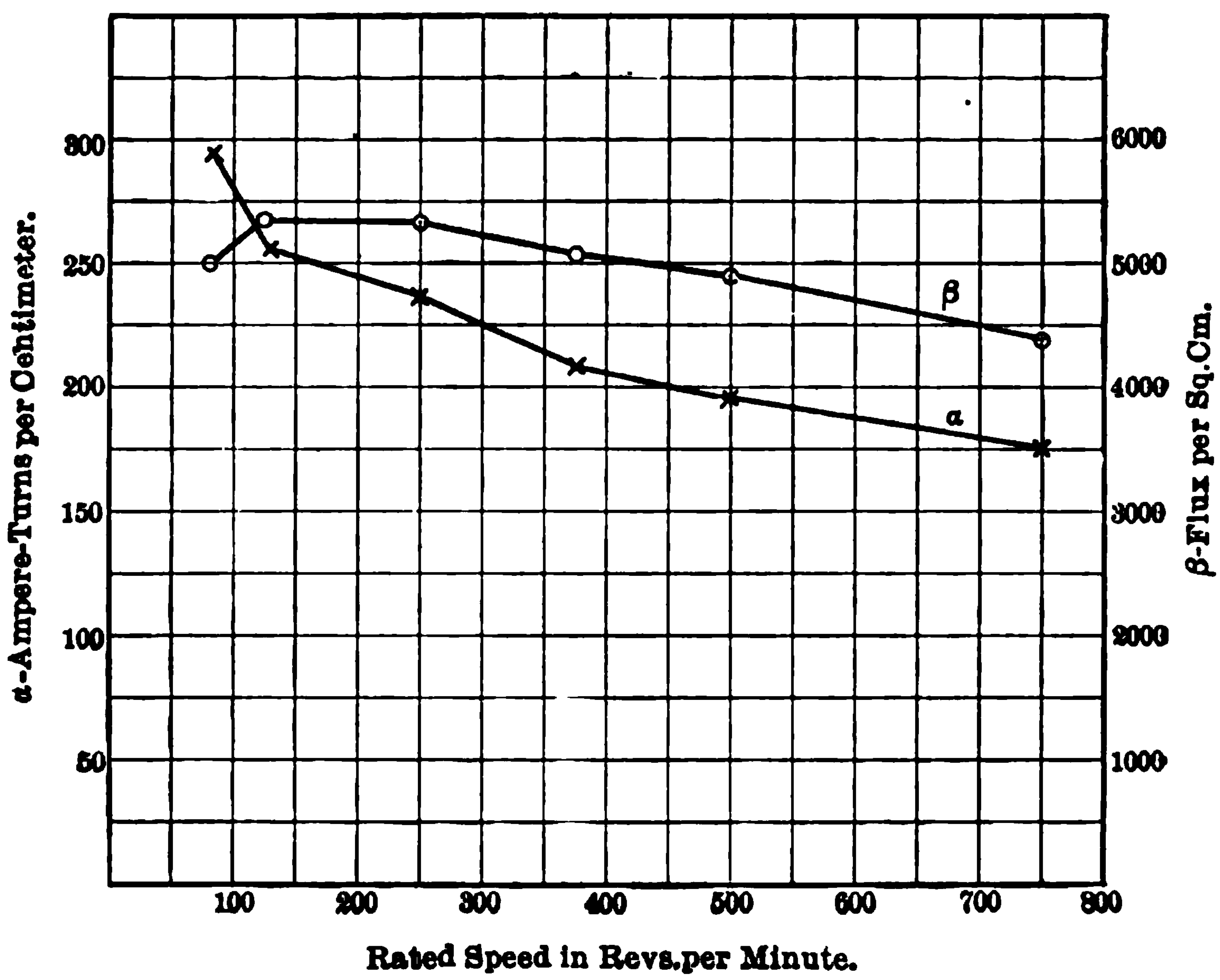


FIG. 96.— Specific Electric and Magnetic Loading α and β for 3000-kva. 25 cycle 3-phase alternator.

Thus in the 83 R.P.M. 36-pole design, the iron loss is twice the copper loss, while in the 750 R.P.M. 4-pole design it is nearly five times the copper loss. In machines of large rated output, such as those we are at present considering, the efficiency is high and the losses are not a sufficiently large percentage of the output for the circumstance to seriously affect the shape of the load efficiency curve. This point, however, is distinctly worth consideration in designs for smaller ratings.

While the total armature losses are practically the same for all these rated speeds, as seen from the curve in Fig. 97, the armature air gap surface is smaller in the higher speed designs, and the heating coefficient is higher. If it were practicable, it would be better to modify

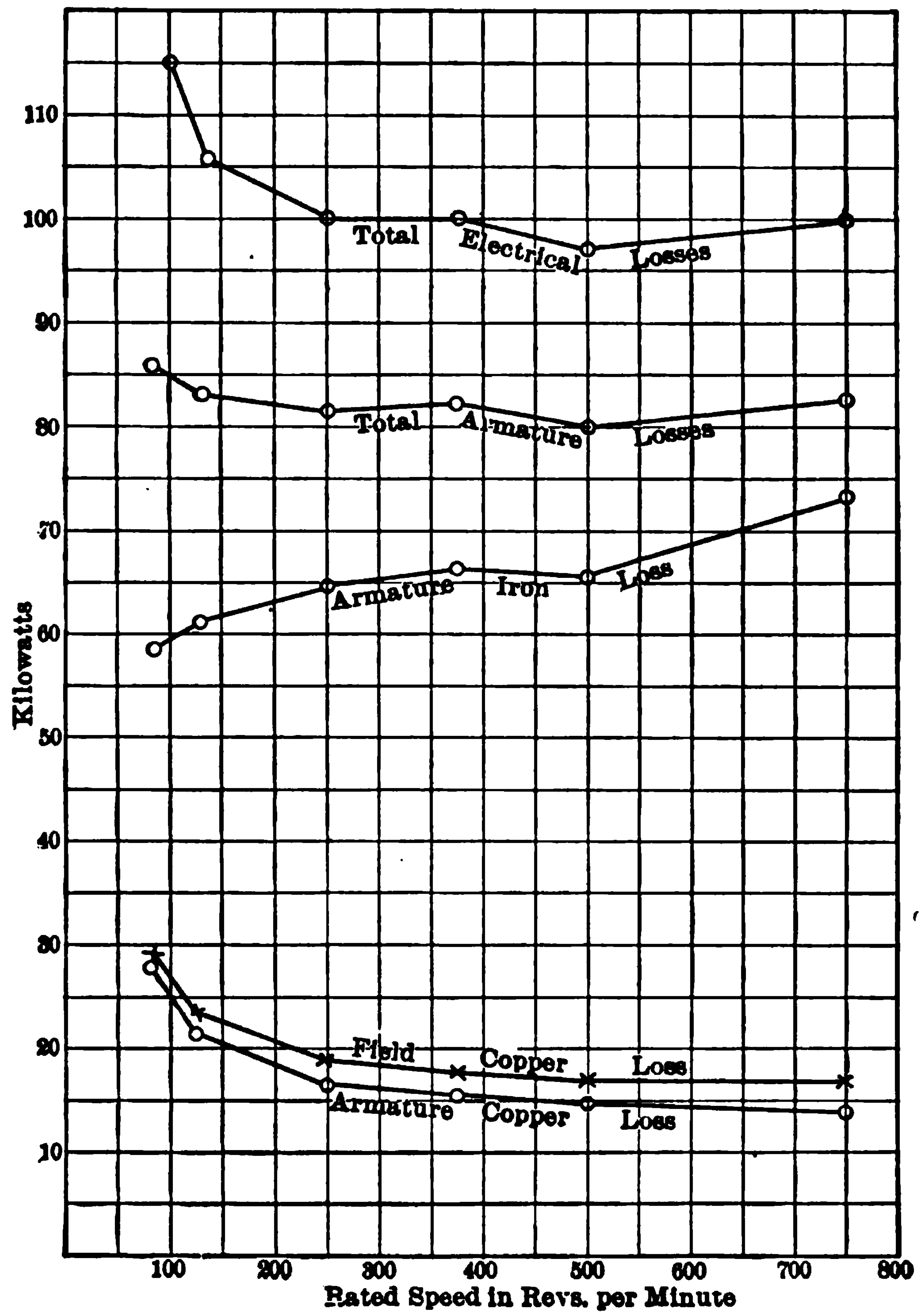


FIG. 97.— Losses for 3000 kva. 25 cycle 3-phase alternator.

the higher speed designs by reducing the armature core loss, and thus obtaining a lower heating coefficient and smaller losses at light loads. This can only be effected by employing a stronger armature and a smaller flux per pole, and in connection with this point the curves in Fig. 96 for the specific electric and magnetic loadings are of interest.

If the working out of the designs is followed through and studied, it will be seen that the suggested improvement cannot be realized, chiefly on account of the limited space available for the field windings in the high speed designs. If a stronger armature were employed in

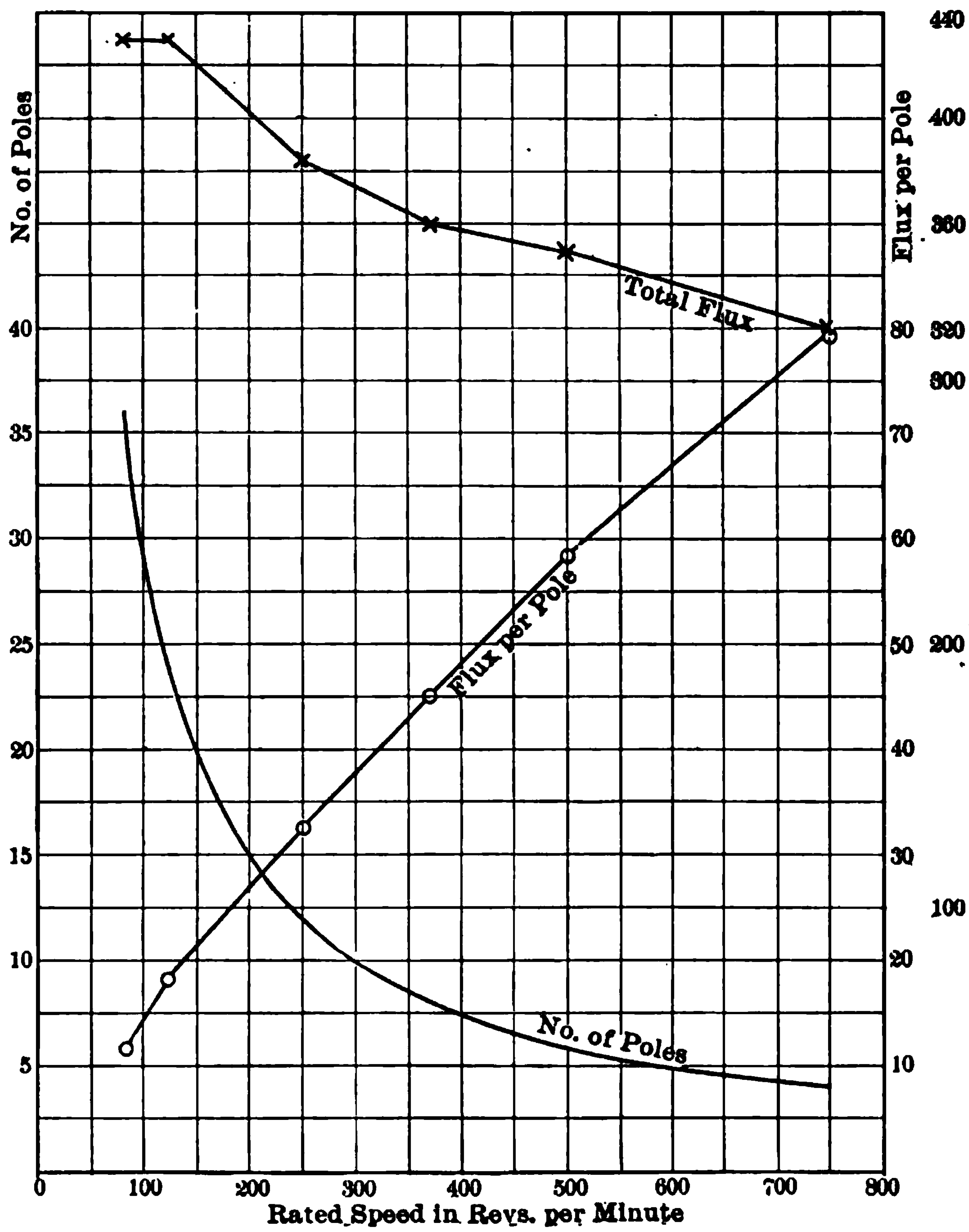


Fig. 98.— Flux per pole and total flux for 3000 kva. 25 cycle 3-phase alternator.

any particular case, the field ampere turns would have to be increased proportionately to the value of α. It is true that the flux per pole will be decreased proportionally, thus permitting of a pole of smaller section and consequently allowing some gain in the winding space. But this gain would not be enough to permit of sufficient

room for the increased amount of field copper without an increase in either or both of the main dimensions D and λg, and a diminution in ξ. An increase in D is not practicable in the high speed designs on account of peripheral speed limitations. An interesting point to note from Fig. 97 is the close approximation, as to magnitude and variation with the speed, of the armature and the field copper losses.

Figs. 98, 99, and 100 give an interesting set of curves showing

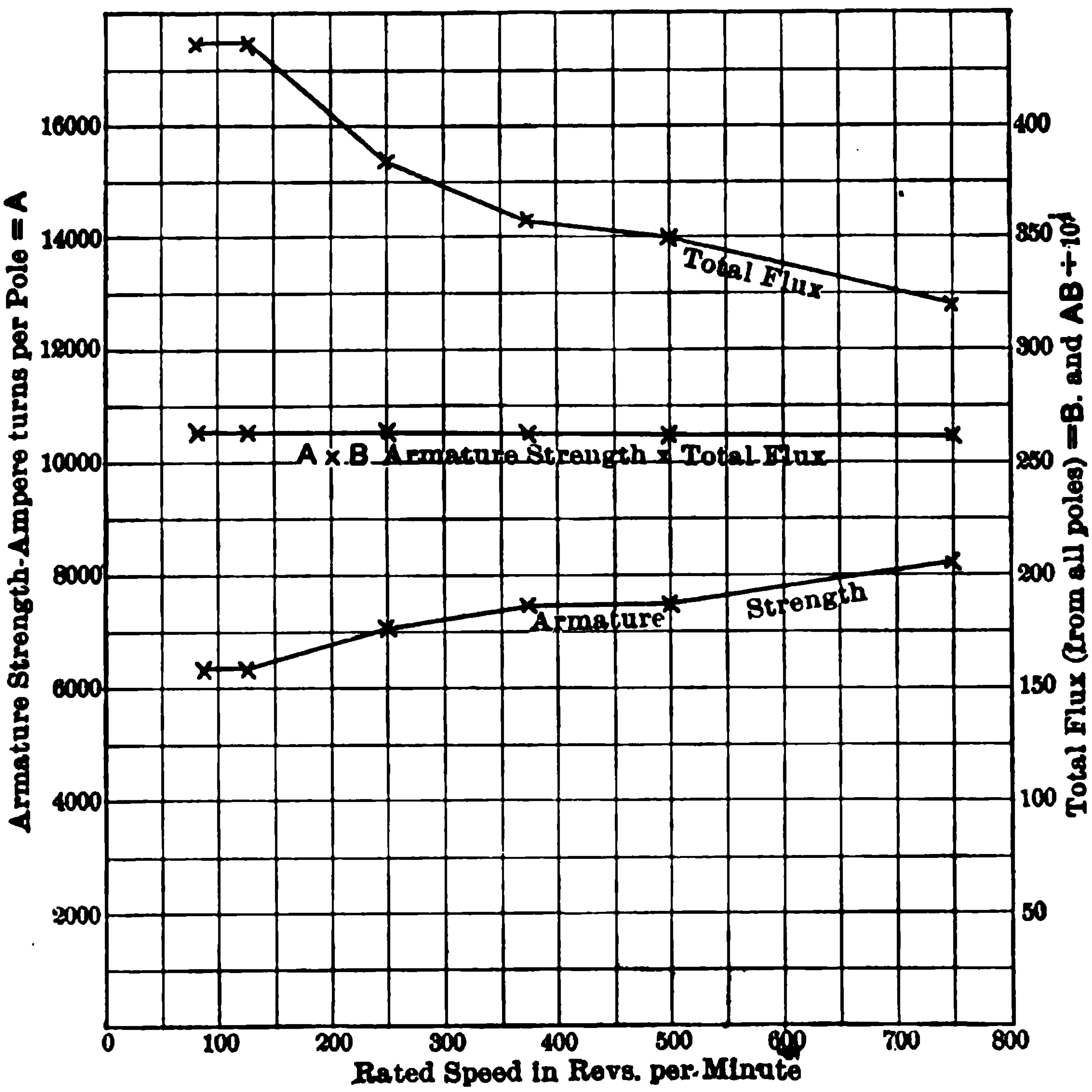


FIG. 99.— Armature strength and total flux for 3000 kva. 25 cycle 3-phase alternator.

the relations obtaining between the armature strength, depth of air gap, flux, and number of poles.

In Fig. 98 is plotted the flux per pole, obtained from the designs, and the number of poles. The latter curve is a hyperbola, as the product of number of poles × speed must be constant. By multiplying the ordinates of these two curves together we obtain a curve for the total flux crossing the air gap from all the poles.

The point to observe is that while the flux per pole increases from

12 megalines in the lowest speed design to 80 megalines in the highest speed design, the total flux crossing the gap varies between comparatively narrow limits, viz., 430 to 320 megalines.

Reference should now be made to the curves for $D\lambda g$ in Fig. 95 and for β, the flux per square centimetre of total air gap surface, in Fig. 96. From these curves we see that $D\lambda g$, which is proportional

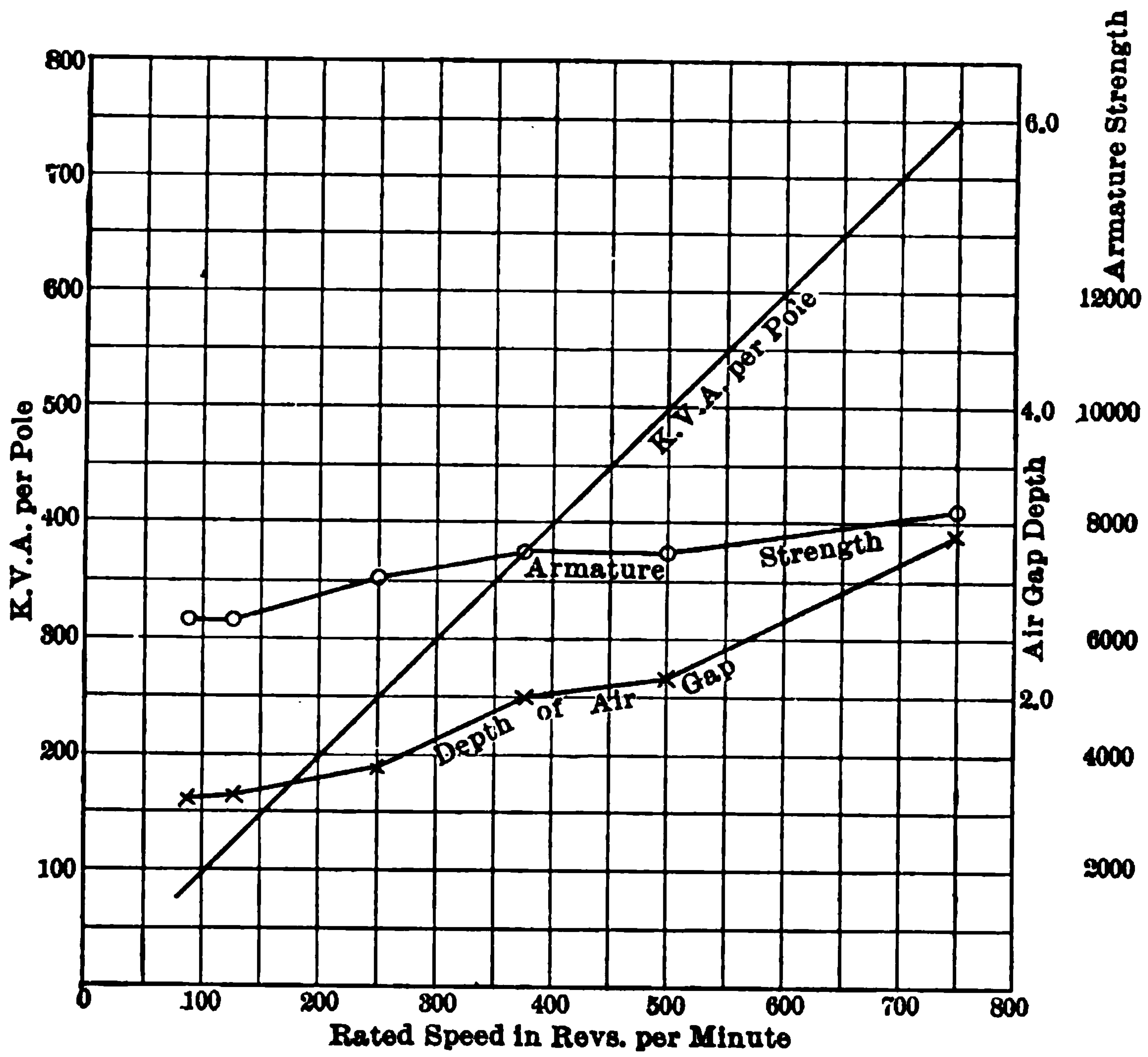


FIG. 100.— Air gap depth and armature strength for 3000 kva. 25 cycle 3-phase alternator.

to the total air gap surface, has a nearly constant value, while β has also a fairly uniform value, decreasing slightly with increasing speed.

The product of $\pi D\lambda g$ and β will also give the total flux which will be quite in agreement with the curve in Fig. 98.

In Fig. 99 the armature strength (ampere turns per pole) is plotted. This quantity, although the number of poles varies widely, does not vary over a wide range. We have reproduced in Fig. 99 the total flux curve from Fig. 98, and have multiplied the total flux by the armature strength. The resultant curve is a straight horizontal line

signifying that the product of armature strength $\times$ total flux is constant.

This may also be seen from the fundamental electromotive force equation

$$V = KTNM.$$

If we multiply both sides by I, the current per phase, and by 3, the number of phases, we have

$$3IV = 3K.ITNM.$$

The term $3IV$ is simply the volt amperes output which is constant in this case, and of the terms on the right hand side K is a constant and N, the frequency, is also constant.

Hence $3IT \times M$ = a constant, i.e., the total armature ampere turns is inversely proportional to the flux per pole M, or

$$3IT \text{ is proportional to } \frac{1}{M}.$$

If we now divide both sides by the number of poles, p, we have

$$\frac{3IT}{p} \text{ is proportional to } \frac{1}{pM}.$$

The term $\frac{3IT}{p}$ is the armature ampere turns per pole or the armature strength. The term pM is the flux per pole multiplied by the number of poles, i.e., the total flux crossing the air gap from all the poles.

Thus the armature strength and the total flux are inversely proportional, as shown by the curves in Fig. 99.

In Fig. 100 there is plotted the radial depth of the air gap, and there is also reproduced the curve for armature strength from Fig. 99.

If all the machines were similar as regards saturation, the length of the air gap would be simply proportional to the armature strength.

We noted on page 161, that the high-speed few-pole machines do not permit of so high a degree of saturation as the low speed multipole designs. Consequently, for the same limits of regulation, the ratio of field ampere turns to armature ampere turns requires to be higher in the high speed machines. The field ampere turns are expended on the air gap and iron of the magnetic circuit, and the air gap accounts for a larger percentage of the total field ampere turns in the high speed machines owing to the lower degree of saturation.

This leads to the result that, in the high speed machines, the field ampere turns are already relatively large, and of them, a large proportion have to be absorbed by the air gap. Further the air gap flux densities are lower in the high speed machines, as β is lower.

It will be seen from the above remarks why such deep air gaps are necessary for high speed generators, and why the depth of air gap increases much more rapidly than the armature strength.

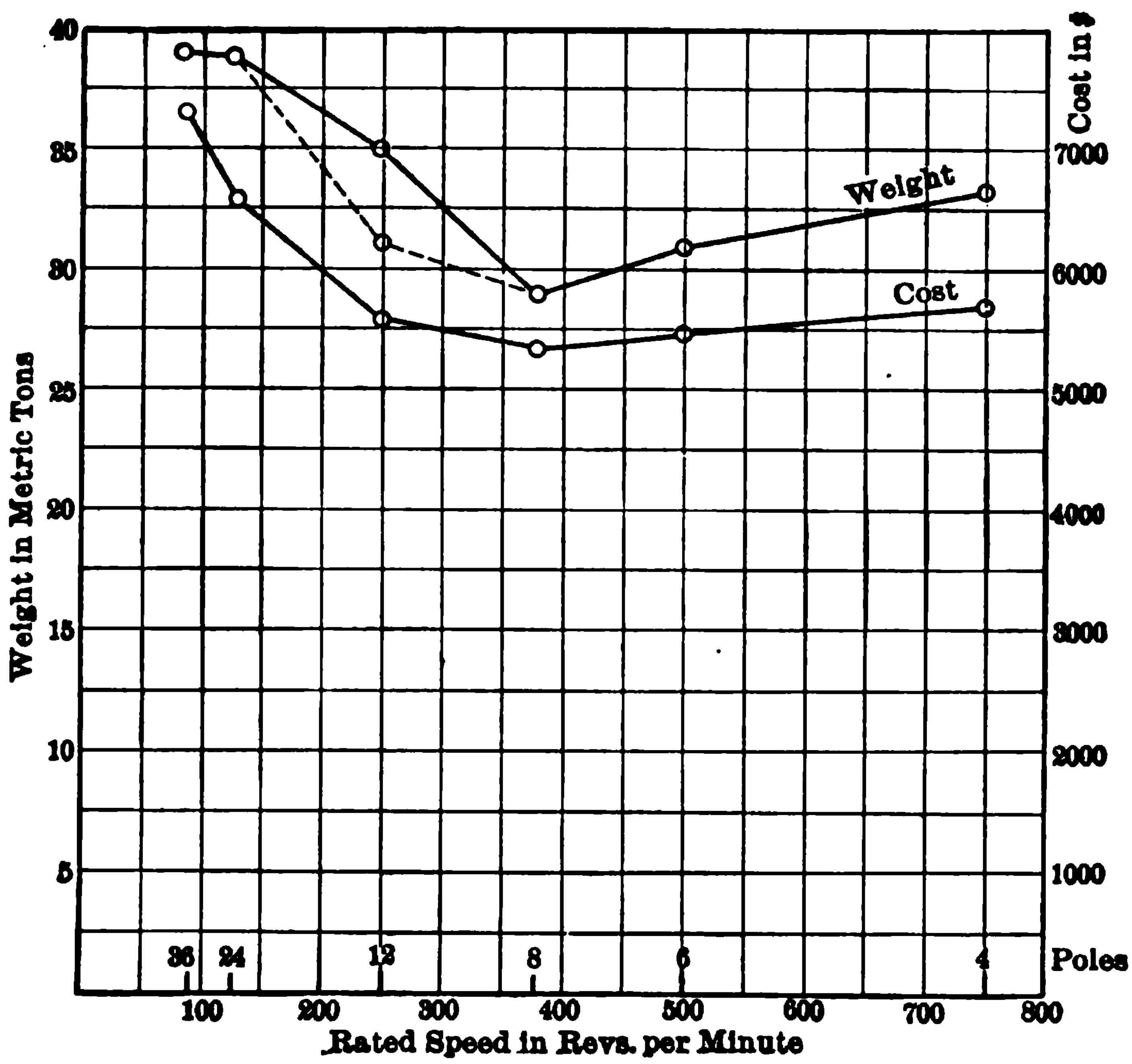


FIG. 101.— Weight and cost of effective material for 3000 kva. 25 cycle 3-phase alternator.

The next matter which is of chief interest in this investigation is that of weight and cost of the materials. At the end of the specification on page 170, there is given a table of weights in metric tons and costs in dollars of the effective or active material. The figures in this table are plotted in the curves of Fig. 101.

Taking first the weight of effective material, the comparatively narrow limits of variation in the weight is remarkable. In the lightest machine the weight of effective material is 28 tons against 39 tons for the heaviest. The latter figure is for the low speed of

83 R.P.M. In this design the magnet yoke is of cast iron. Were a wrought iron or steel yoke employed, to make a more strictly fair comparison with the other machines, the weight of effective material would be reduced to about 33 tons. This is indicated by the dotted curve in Fig. 101, which shows the weights of effective material for the 250 R.P.M. machine based on the use of a wrought iron or steel yoke. On the latter basis the highest weight is only some 20 per cent greater than the lowest weight. The values of $D^2\lambda g$ are for these two cases 13.5 and 4, and these values are in the ratio 3.4 : 1.

The values of the product $D\lambda g$, which are plotted in Fig. 95, follow much more closely the relative values of the weight of effective material. In the following table we have set forth the weights of effective material and the total weights of the machines, and the "weight coefficients" which are defined as the total weight per unit of $D\lambda g$ and $D^2\lambda g$ respectively. The total weight of each machine is estimated from the weight of effective material by assuming a value for the ratio of total net weight to weight of effective material. We may designate this the "weight factor." (See p. 19, Chap. II.)

The values taken for the "weight factor" range from 2.0 in the 750 R.P.M. 4-pole machine to 2.8 in the 83 R.P.M. 36-pole machine. Larger values must be taken in the case of low speed flywheel machines on account of the large amount of inactive material present in the magnet wheel and stator frame. (See Fig. 10, on p. 18.)

TABLE 38.

WEIGHT COEFFICIENTS OF 3000 KVA. ALTERNATORS.

Number of Poles	4	6	8	13	24	36
Speed R.P.M.	**750**	**500**	**375**	**250**	**125**	**83**
Weight of effective material — tons	33.1	31.3	27.7	30	38.3	38.7
Estimated total weight of machine — tons	66	63	61	72	100	110
D and λg in metres	2.4	2.33	2.23	2.3	2.58	2.75
D^2 and λg in metres	2.85	3.5	4.0	5.3	9.8	13.7
$\frac{\text{Weight of effective material}}{D\lambda g}$	13.8	13.5	12.4	13.0	14.8	14.0
$\frac{\text{Total weight}}{D\lambda g}$	27.5	27.0	27.4	31.3	38.7	40.0
$\frac{\text{Weight of effective material}}{D^2\lambda g}$	11.6	9.0	7.0	5.7	3.9	2.8
$\frac{\text{Total weight}}{D^2\lambda g}$	21.0	18.0	15.3	13.6	10.2	8.0

The results of the above table are plotted in Fig. 102. The point to be observed from the curves in Figs. 101 and 102 is that the weights and costs of effective material and the total weights pass through minimum values. The speed at which this occurs, so far as concerns material, corresponds nearly to the 375 R.P.M. 8-pole design. Between the 8-, 6-, and 4-pole designs, there is very little difference, although the speed of the 4-pole machine is double that of the 8-pole. It is probable that the 8-pole design is not, however, the one corresponding to minimum Total Works Cost as this machine has a fairly

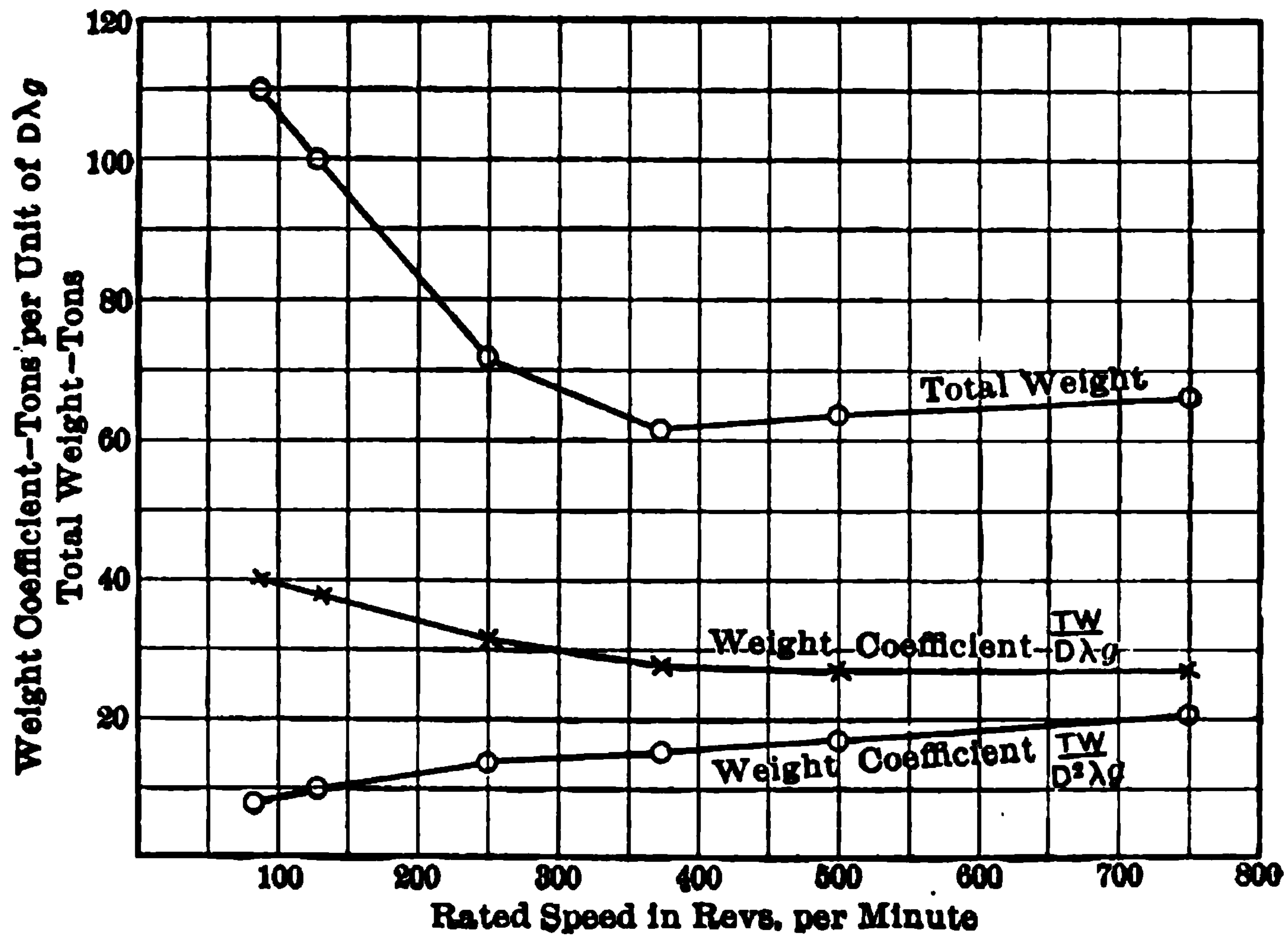


FIG. 102.— Total net weights and weight coefficients for 3000 kva. 25 cycle 3-phase alternators.

large diameter and the labour charges are greater than in the machines with fewer poles and of smaller diameters. The 6-pole rather than the 8-pole design would probably have the minimum Total Works Cost.

The total weight curve in Fig. 102 shows very little difference in weight between the 4-, 6-, 8-, and 12-pole machines. It is interesting to compare these results with those arrived at by Behrend, as shown in the curve of Fig. 66, Chapter VI. The following table shows the weights of Behrend's machines side by side with those of the designs contained in this chapter.

TABLE 39.

SHOWING TOTAL WEIGHT OF 25 CYCLE 3-PHASE ALTERNATORS FOR 1000 KVA. (BEHREND) AND 3000 KVA. (AUTHORS).

Rated Speed in R.P.M.	Total Weight in Metric Tons.	
	1000 kva.	3000 kva.
83	...	100
100	37	...
125	...	110
250	25	66
375	...	61
450	20	...
500	...	62
750	(17)	66
1500	26	...

Behrend's figures show that the design corresponding to minimum weight for a 1000 kva. 25 cycle machine is a 4-pole 750 R.P.M. design, which would have a weight of about 17 tons. A machine for 1500 R.P.M. (2-pole) weighed 26 tons, this being some 50 per cent heavier. In the case of the larger rating, 3000 kva., the designs corresponding to minimum weight are the 500 R.P.M., 6-pole, or the 375 R.P.M., 8-pole. Thus the most economical speed for the larger ratings is lower than that for the smaller ratings.

We see from Fig. 102 that the total weight is already distinctly increasing in the 6 and 4-pole machines, and were a 3000 kva. design carried out for 2 poles and 1500 R.P.M. it would be found to be much heavier and more expensive than any of the lower speed designs. Such a design for 3000 kva. is an uncommercial proposition, and by way of further indicating its impracticability we have roughed out below the leading dimensions which would be necessary for the design to be anywhere near a rational proposition. The fact that such machines may be advocated and even planned and built by certain ill-advised manufacturers, does not affect our statement.

The following figures obtained from the rough outline design, embody the principal data for the design for such a rating. The data for the 1500 R.P.M. 2-pole design is here tabulated side by side with the 750 R.P.M. 4-pole design, for purposes of comparison.

Output in kva.	3,000	3,000
Terminal voltage	11,000	11,000
Speed R.P.M.	1,500	750
Frequency	25	25
Number of poles	2	4
Armature Iron		
Diameter at air gap D	100	120
External diameter	230	196
Gross core length λg	150	198
Net core length λn	88	138
Total number of slots	48	60
Number of slots per pole per phase	8	5
Armature Copper		
Number of conductors per slot	8	7
Current density	300	280
Number of turns in series per phase	64	70
Armature strength — ampere turns per pole	15,000	8,250
Rotor Iron		
Diameter at pole face	88	114
Pole pitch at air gap	157	94
Gross axial length	150	198
Radial length of air gap	6.0	3.0
Peripheral speed	79	47
Field Copper		
Ampere turns per pole at no load	30,000	16,500
Ampere turns per pole at full load and $\cos \phi = 0.8$	42,000	23,000
Armature flux per pole — megalines	98	81
Losses Efficiency and Heating		
Losses in kw.		
Armature I^2R total	16	14.1
Armature iron total	69	68.5
Total armature losses (iron + copper)	85	82.5
Field loss at full kva. and $\cos \phi = 0.8$	17	17.2
Total electric and magnetic losses	102	100
Efficiency (exclusive of friction) at full load	97.0	97.0
Heating		
Watts per sq. dcm. of armature surface		
A. Calculated on $\pi D\ (\lambda g \times 0.7\tau)$	104	83
B. Calculated on $\pi D \lambda g$	180	110
C. Calculated on $\pi D\ (\lambda g + K\tau)$	67	63
D. Calculated on total surface	37	33
Approximate Weights of Effective Material in Tons		
Field iron	8.0	11.0
Armature iron	22.5	19.0
Total effective iron	30.5	30.0
Field copper	0.7	0.8
Armature copper	2.3	2.3
Total effective copper	3.0	3.1
Total effective material	33.5	33.1
Constants and Coefficients		
$D^2\lambda g$ (D and $\lambda\gamma$ in metres)	1.5	2.85
$D\lambda g$ (" " " " ")	1.5	2.4
$D/\lambda\gamma$	0.66	0.6
$\lambda\gamma/\tau$	1.0	2.1
Output coefficient ξ	1.35	1.4
Ampere conductors per cm. a	192	175
Flux per sq. cm. β	4,300	4,350
Kva. per pole	1,500	750

The comparison which may be drawn from the above tabulation is necessarily only rough. Nevertheless it shows that the 1500 R.P.M. 2-pole design is no less expensive than the design for half the rated speed viz., 750 R.P.M. and 4 poles. Moreover, the design for 750 R.P.M. is much more satisfactory than the one for 1500 R.P.M. The weight and cost of the material for the two designs as shown in the above table come out practically the same, although the rated speed in one case is twice that of the other.

There is a remarkable similarity in the distribution of the material in the various parts of the machine. The principal difference is in the armature laminations, which weigh 22.5 tons for the 1500 R.P.M. and 19 tons for the 750 R.P.M. If special grade low-loss iron is employed, as would be highly desirable if such a machine as the 2-pole 1500 R.P.M. rating were required to be constructed, the cost of the armature laminations constitutes a large proportion of the total cost (see curves in Figs. 34 to 50 of Chapter IV).

The Total Works Cost of the two machines may be about the same; and although from this standpoint the 2-pole design may not be disadvantageous, the 4-pole design is decidedly preferable from both the electrical and the mechanical standpoints.

The peripheral speed is necessarily much higher in the 2-pole design. This gives rise to high stresses and renders the design of the rotor a difficult problem. The construction would also call for a large expenditure for labour. In addition, the construction and arrangement of the windings with a 2-pole field are not nearly so satisfactory as with a 4-pole, and the balancing of the former is much more uncertain.

A comparison of the electrical quantities shows the heating to be much greater in the 2-pole design, which calls for even more vigorous ventilation. This greater heating is due principally to the large armature core loss and to the cramping of the field copper winding space owing to the small diameter.

Regarding the pressure regulation, we have not calculated this for the 2-pole design, but it will be much inferior to the 4-pole design on this point. We have already noted in Chapters V and VIII, pages 81 and 141, the bad regulating qualities of 2-pole machines, and the observations there made apply also to the present case. In fact, the difficulties are accentuated, as we have here to deal not only with a high speed but also at the same time, with a large output.

The 2-pole design is a very difficult one and is necessarily of poor

quality. While possessing serious disadvantages as to quality and operation, it has not, to offset these, any economy in material over designs for lower rated speeds with more poles.

The result does not depend so much on the actual rated speed as on the number of poles, and thus indirectly on the frequency, as the latter depends on the speed and number of poles.

Designs for 50 Cycles.

By way of further illustrating this subject and of making quantitative comparisons, we have extended the group of designs worked out in this chapter by the addition of 3000 kva. designs for 50 cycles at the following rated speeds and numbers of poles:

Number of poles	8	6	4
Speed R.P.M.	750	1000	1500

We shall see from these designs that a 1500 R.P.M. machine for 50 cycles and 4 poles is a reasonable proposition, although a 1500 R.P.M. 25 cycle design is almost out of the question, since it must have two poles. This is, as we shall see, simply dependent on the number of poles, the 4-pole design being quite practicable while the 2-pole is unreasonable. Both of these designs are for the same rated speed of 1500 R.P.M. These three additional 50 cycle designs may be compared with the 25 cycle designs of either the same speed or the same number of poles.

SPECIFICATION FOR
3000 Kva. 3-Phase 4-, 6-, 8-Pole
1500, 1000, 750 R.P.M. 50 Cycle 11,000 Volt
ALTERNATING CURRENT GENERATORS.
All Dimensions in Cms.

	A.	B.	C.
Output in kva.	3,000	3,000	3,000
Terminal voltage	11,000	11,000	11,000
Style of connection	Y	Y	Y
Current per terminal	157	157	157
Speed R.P.M.	1,500	1,000	750
Frequency	50	50	50
Number of poles	4	6	8
ARMATURE IRON			
Diameter at air gap . . . D	110	130	150
Diameter at bottom of slots	194	196	222
Depth below slots	37	27.5	30
Gross length between core-heads . . . λg	125	135	140
Number of ventilating ducts	22	24	28
Width of each duct	1.25	1.25	1.25
Effective core length (iron) . . . λn	87	94.5	95

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.
Slots and Teeth			
Total number of slots	72	90	120
Slot pitch at armature face	4.8	4.92	3.9
Width of slot	2.7	2.6	2.1
Width of tooth at armature face	2.1	1.93	1.83
Radial depth of slot	5.0	5.5	6.0
Armature Copper			
Number of slots per pole per phase	6	5	5
Number of conductors per slot	6	5	4
Section of conductor	0.895	0.734	0.65
Current per conductor	158	158	158
Current density — amps. per sq. cm.	177	215	240
Dimensions of conductor bare	0.9×1.33	2×(0.77 dia.)	2×(0.45 ×0.965)
Thickness of slot insulation	0.5	0.5	0.5
Number of turns in series per phase	72	75	80
Armature ampere turns per pole	8,500	5,900	4,750
Rotor Iron			
Diameter at pole face	102.5	125	147
Length of air gap	3.75	2.5	1.5
Pole pitch at air gap	86	68	59
Circumferential length of pole arc	58	45	39
Ratio pole arc / pole pitch	0.66	0.67	0.66
Gross axial length (parallel to shaft)	125	135	140
Effective axial length of pole (iron)	125	121	126
Breadth of pole body across shaft	24	21	25
Field Copper			
Total length of winding space	19	22	22
Number of turns per pole	144	74	50
Width of strip	3.88	3.75	3
Thickness of strip	0.2	0.229	0.31
Total cross section of winding space per pole	148	82.5	66
Total cross section of copper	112	64.5	45
Space factor	0.76	0.78	0.68
Current at full load and $\cos\phi = 0.8$	155	218	260
Current density amperes per sq. cm.	200	254	280
Resistance of all field spools in series at 60 degrees Cent.	0.473	0.34	0.303
Volts across field at above current	75	75	75
Exciter voltage	100	100	100
Magnetic Data			
Armature flux per pole (megalines)	39	36	34
Leakage coefficient (calculated at no load)	1.1	1.11	1.14
Flux in the pole core	43	40	39
Magnetic Densities in Kilolines Per Sq. Cm.			
Armature	6	7	6
Teeth	17.2	16.5	18
Pole core	15.5	15.5	15.3
Mean density in air gap	6	6	6.76
Ampere Turns			
Armature	75	125	80
Teeth	300	190	540
Gap	16,500	11,500	8,100
Pole core	450	540	500
Total no load ampere turns	17,300	12,300	9,200

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.
Losses			
Armature Copper			
Mean length of turn	594	540	516
Resistance per phase at 60 degrees Cent.	0.0957	0.11	0.126
Total copper loss at full load in kw. $\cos\phi=1$	7.2	7.2	9.4
Armature Iron			
Weight of armature iron (excluding teeth) — tons	12.4	10.4	13.4
Frequency	50	50	50
Flux density — kilolines	6	7	6
Kilowatts per ton	3.95	5.4	3.95
Total core loss — kw.	49	56	53
Teeth			
Weight of teeth — tons	0.480	0.77	1.07
Flux density — kilolines	17.2	16.5	18
Kilowatts per ton	33	30	35
Total tooth loss — kw	16	23.2	37
Total iron loss — kw	65	79.2	90
Iron loss + copper loss ($\cos\phi=1$)	72.2	86.4	100
Watts per sq. decimetre of armature air gap surface $\cos\phi=1$	113	115	117
Watts per sq. dcm. of total surface	45	50	48
Field Copper			
Power in kw. for excitation at full load $\cos\phi=1$	10	11	15.7
Do. $\cos\phi=0.8$	11.6	16.5	19.5
Watts per sq. dcm. of external surface of field spool ($\cos\phi$ 0.8)	45	36	31
Efficiency			
At full load and power factor = 1.0			
Armature copper loss	7.2	7.2	9.4
Armature iron loss	65	79.2	90
Field copper loss	11.4	16	20
Total calculable losses	82	103	120
Efficiency (exclusive of friction)	97.5%	97%	96.5%
Regulation			
Inherent regulation at full load and power factor = 1.0	5.5%	6.5%	7.5%
At full load and power factor = 0.8	20.5%	20.5%	17.5%
Constants and Coefficients			
Weight of effective material — tons	19.8	20.7	23.5
Weight of effective material per kw.	0.066	0.069	0.0785
Cost of effective material — $	3,500	3,300	3,500
Cost of effective material per kw.	1.17	1.1	1.17
$D\lambda g$ (*D* and λg in meters)	1.37	1.75	2.1
$D^2\lambda g$ metres (*D* " " " ")	1.51	2.28	3.15
Ratio $\lambda g/\tau$	1.45	2	2.38
Ratio $D/\lambda g$	0.88	0.99	1.07
Output coefficient ξ	1.32	1.3	1.22
Ampere conductors per cm. of periphery a	196	176	106
Flux per sq. cm. of armature surface β	3,600	3,920	4,130
Peripheral speed ∂ metres per second s	78.5	68	59
Watts per cu. cm. of active belt	13.3	9.4	7.5
Ratio of field ampere turns to armature ampere turns	2.05	2.08	1.95
Kva. per pole	750	375	188

WEIGHT AND COST OF EFFECTIVE MATERIAL.

	Tons.			Dollars.		
	A.	B.	C.	A.	B.	C.
Magnet cores	1.8	3.3	3.5	220	410	430
Magnet shoes	0.8	0.8	1.25	100	100	160
Magnet yoke	2.3	3.9	7.1	290	490	890
Armature laminations	12.9	11.2	14.5	1610	1400	1810
Total iron	17.8	19.2	26.3	2220	2400	3290
Armature copper	1.03	0.8	0.72	645	500	450
Field copper	1.42	1.13	1.1	890	710	690
Total copper	2.45	1.93	1.82	1535	1210	1140
Total effective material	20.2	21.2	28.1	3735	3600	4400
Estimated total weight	40.0	42.0	56.0			

PERCENTAGE WEIGHT AND COST OF EFFECTIVE MATERIAL.

	Percentage Weight.			Percentage Cost.		
	A.	B.	C.	A.	B.	C.
Magnet cores	9.0	15.6	12.3	6.4	11.4	9.7
Magnet shoes	4.0	4.0	4.5	2.8	2.8	3.6
Magnet yoke	11.0	18.6	25.3	8.1	13.7	20.0
Armature laminations	64.0	52.8	51.4	46.1	38.7	40.9
Total iron	88.0	91.0	93.5	63.4	66.6	74.2
Armature copper	5.0	3.8	2.6	11.3	13.8	10.2
Field copper	7.0	5.2	3.9	25.3	19.6	15.6
Total copper	12.0	9.0	6.5	36.6	33.4	25.8
Total effective material	100.0	100.0	100.0	100.0	100.0	100.0

The designs for these machines are set forth in the above specification with an analysis of weights and costs of the material, and the leading coefficients and constants.

Outline sketches of the machines are given in Figs. 103 to 106 where is also reproduced to the same scale the 750 R.P.M. 25 cycle 4-pole design from Figs. 93 and 94. Figs. 103 and 104 appear side by side enabling a comparison to be made between the two machines both having 4 poles, but for 750 R.P.M. 25 cycles and 1500 R.P.M. 50 cycles respectively. We have also grouped together Figs. 104, 105 and 106 to show comparisons between the designs for different speeds and numbers of poles for the same rated output and frequency.

Fig. 103 also appears directly above Fig. 106, constituting a comparison between two machines having the same speed 750 R.P.M. but for 25 cycles 4 poles, and 50 cycles 8 poles respectively.

In Figs. 107–111 we have plotted the same quantities as were plotted in Figs. 95–102 for the 25-cycle designs. These curves are of interest as showing the comparisons between these three machines for high speeds, and also as showing the effect of going to the highest speed practicable for a machine of this rating. Later on in Figs. 112–

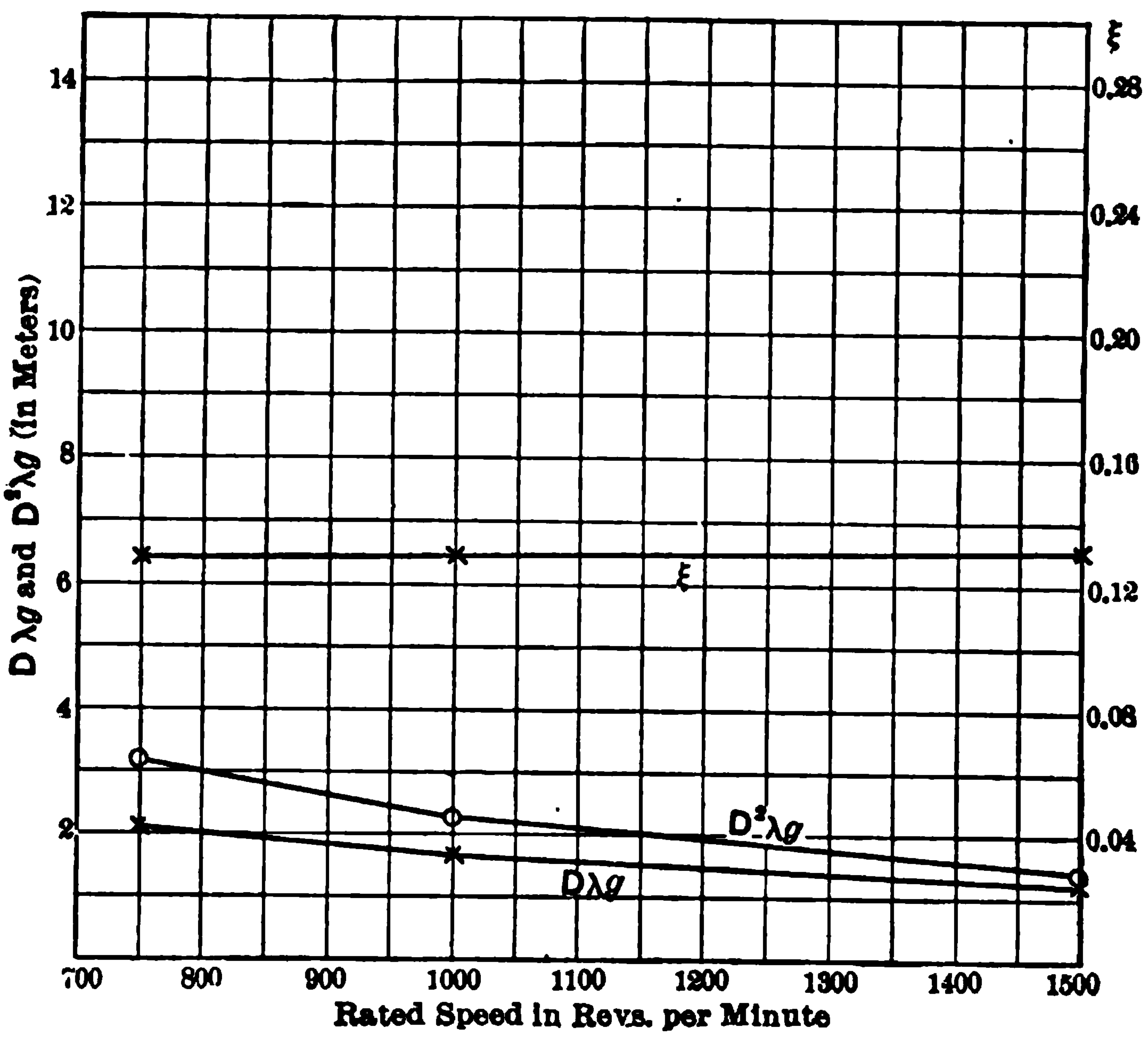


FIG. 107.— $D\lambda g$, $D^2\lambda g$ and output coefficient for 3000 kva., 50-cycle, 1-phase alternator.

115 we have thrown together the two sets of curves for the 25 and 50 cycle machines, thus covering a range of speed from 83 to 1500 R.P.M.

It is notable from Figs. 107 and 108 that while the value obtained for the output coefficient, ξ, is practically the same in all three cases, the relative values for α and β, the electric and magnetic loadings, differ considerably The curves in Fig. 108 show that the higher the speed the greater is the value of β relative to α. This may be as-

1500 R. P. M.

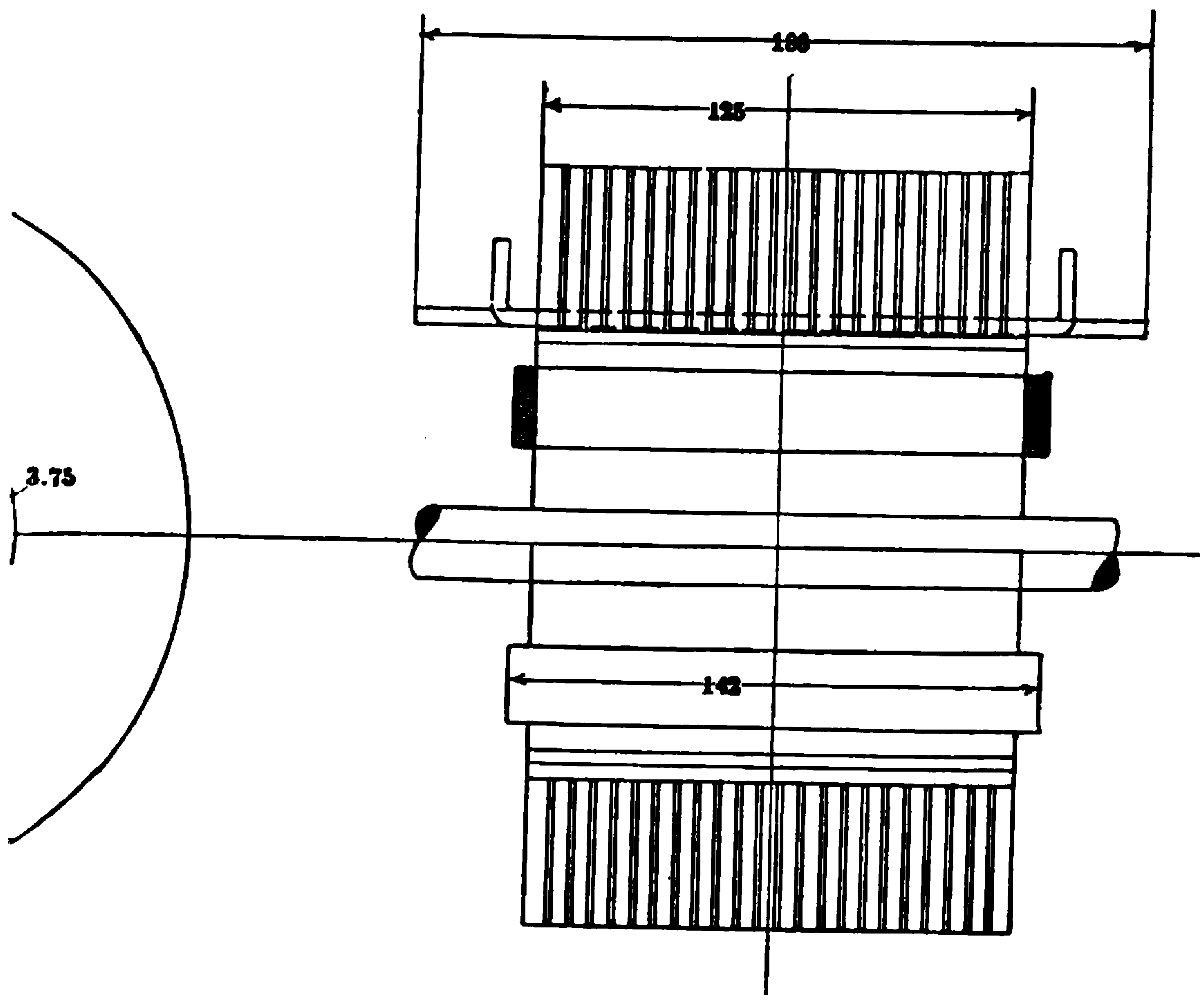


Fig. 104.

4 POLE, 1

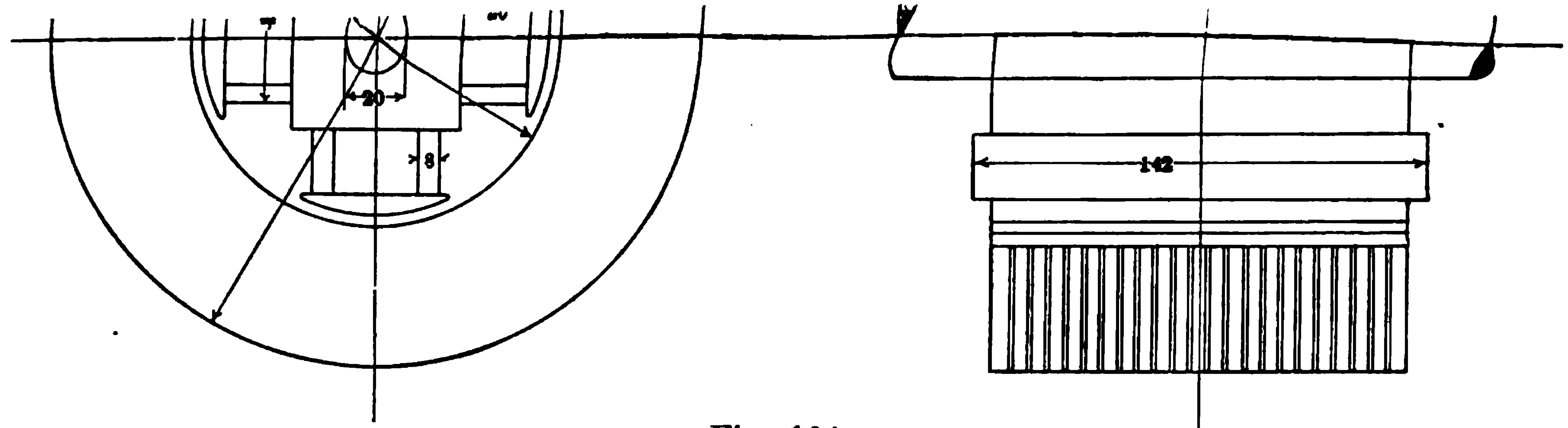


Fig. 104.

Figs. 104, 105 and 106

OUTLINE DRAWINGS OF 3000 KVA. 50 CYCLE ALTERNATORS

Fig. 104—1500 R.P.M. 4 Poles
Fig. 105—1000 " 4 "
Fig. 106—750 " 8 "

cribed to the fact that smaller values of the flux per pole (and consequently stronger armatures) are the more desirable the fewer the poles, in order to keep down the weight of armature iron and the armature iron loss and heating. This point is not so marked in Fig. 96 for the 25-cycle designs, as we are here dealing with lower speeds. In these cases the armatures are of larger dimensions, and the heating problem is not so emphasised as in the case of very high speeds.

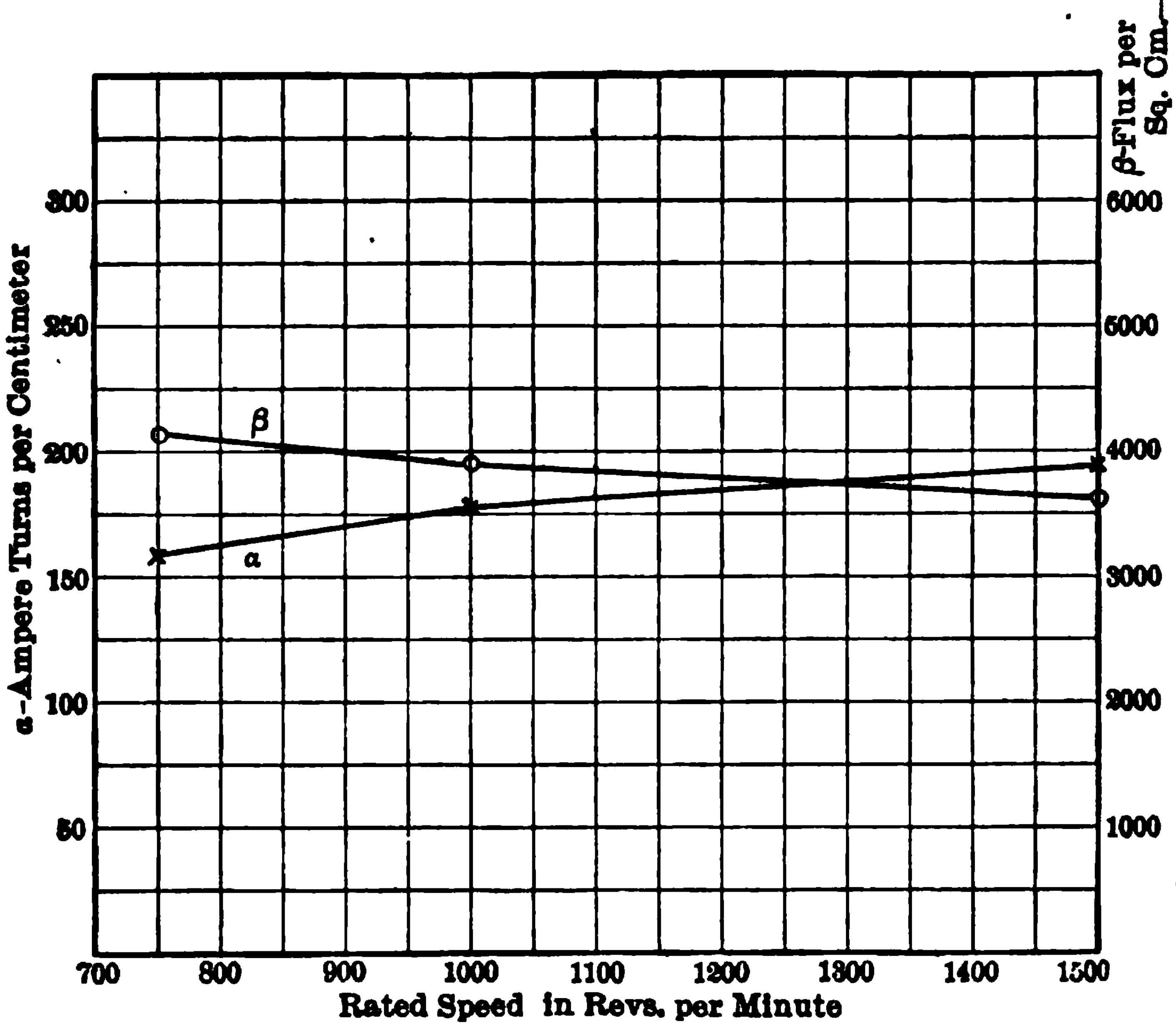


FIG. 108. — Specific magnetic and electric loading, β and α, and for 3000 kva. 50-cycle 3-phase alternators.

In Fig. 109 are plotted the various losses. Comparing with Fig. 97, the iron losses in the 50-cycle machines are greater than in the 25 cycle machine on account of the greater frequency. The armature and field copper losses are less as the mean lengths of the turns are smaller on account of the small overall dimensions.

It is notable that the total electric and magnetic loss shows a falling off in the higher speeds. This is because the speed range for these three machines is very great, (from 750 to 1500 R.P.M.), and it is necessary, in the very high speed designs, to keep down the losses on account of the thermal limitations.

Fig. 110 shows curves for the weight and cost of the effective material. From these curves it will again be observed that doubling the rated speed from 750 to 1500 R.P.M. decreases the cost of effective material only from $4400 to $3500, or only about 20 per cent.

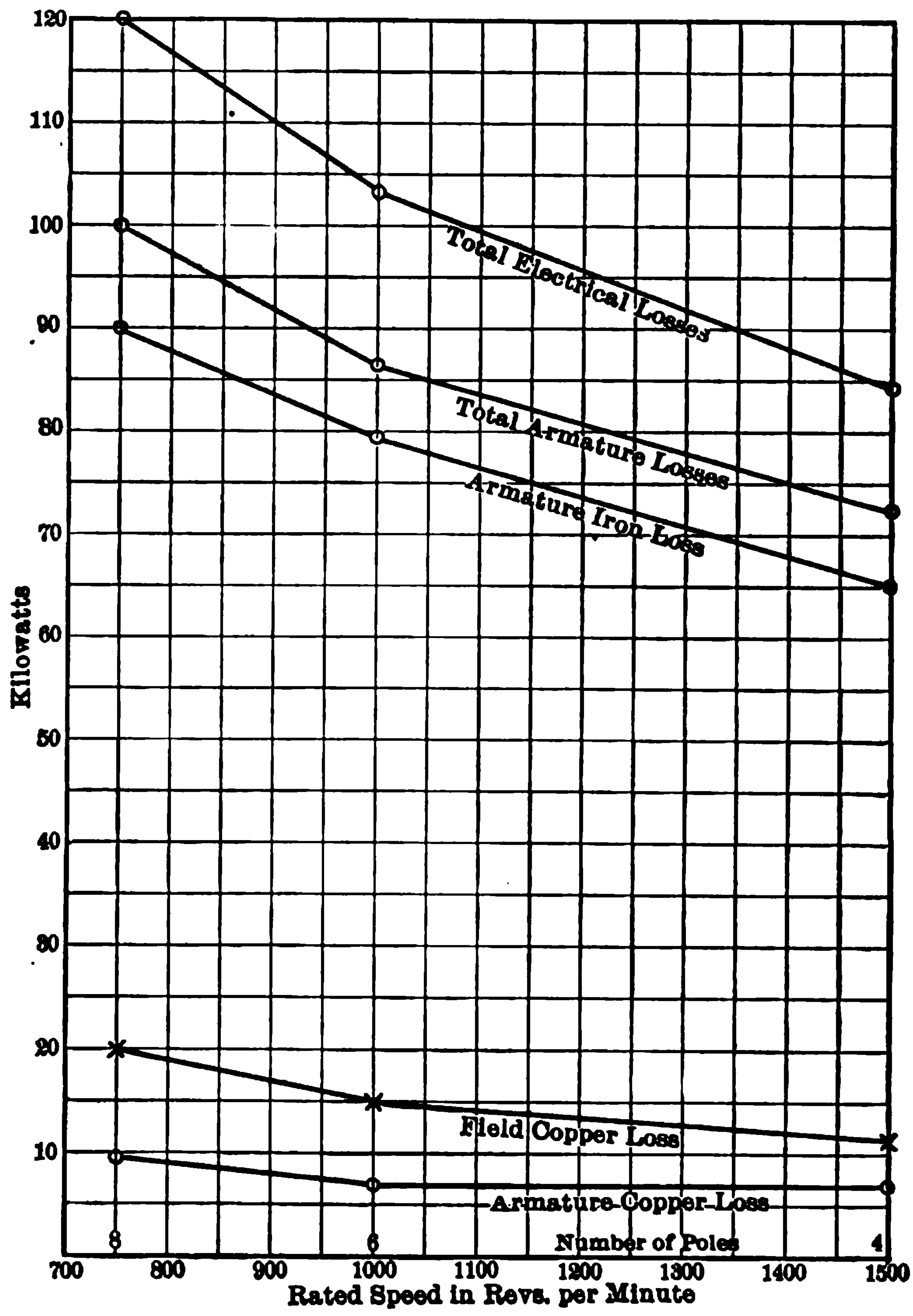


FIG. 109.— Losses for 3000 kva. 50 cycle 3-phase alternator.

The curves also show that the 4-pole design corresponds to the limiting economical speed beyond which designs will be more expensive and poorer in quality. The 6-pole 1000 R.P.M. and the 4-pole 1500 R.P.M. designs are practically identical so far as economy of

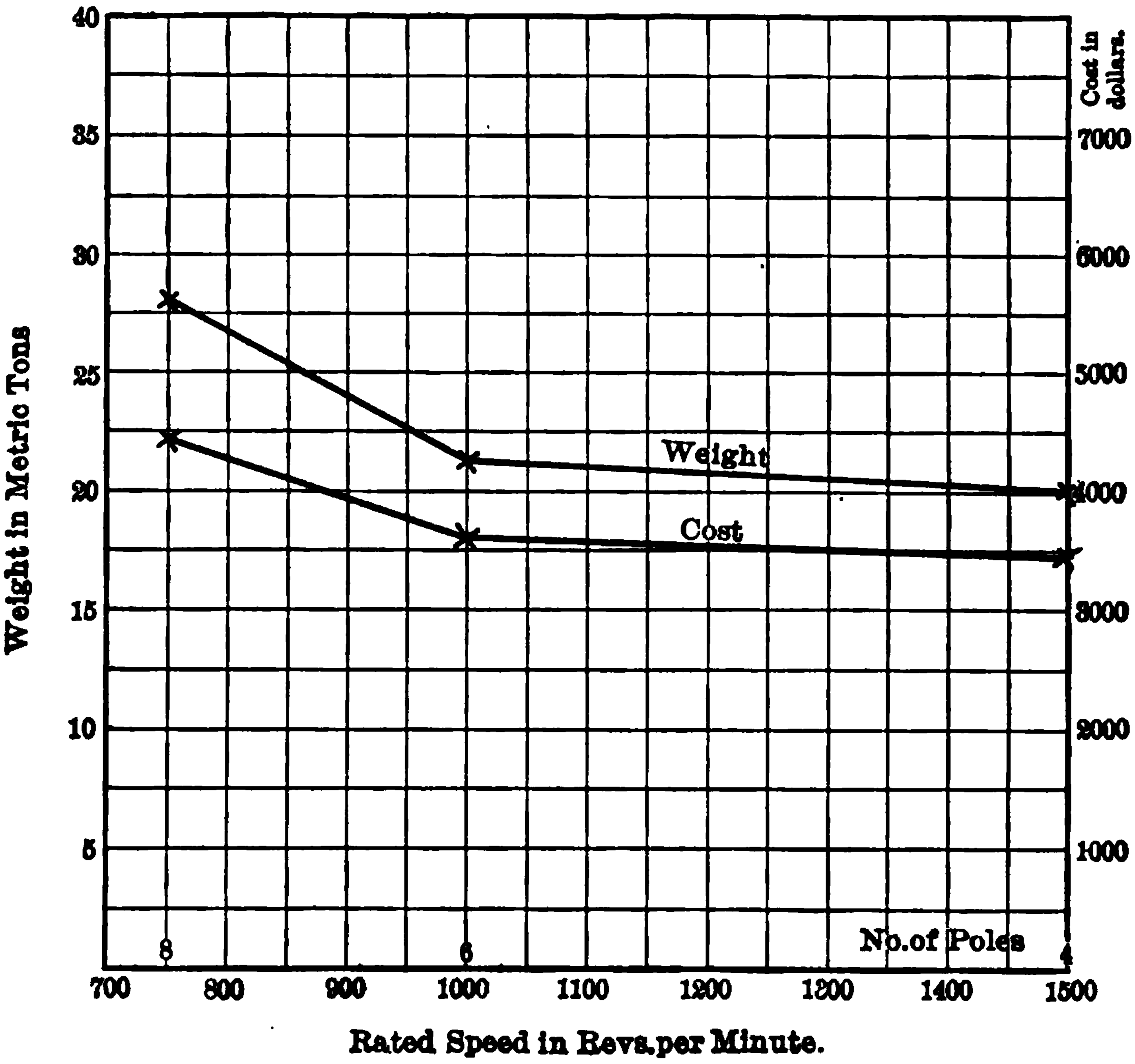


FIG. 110.— Weight and cost of effective material for 3000 kva. 50 cycle 3-phase alternator.

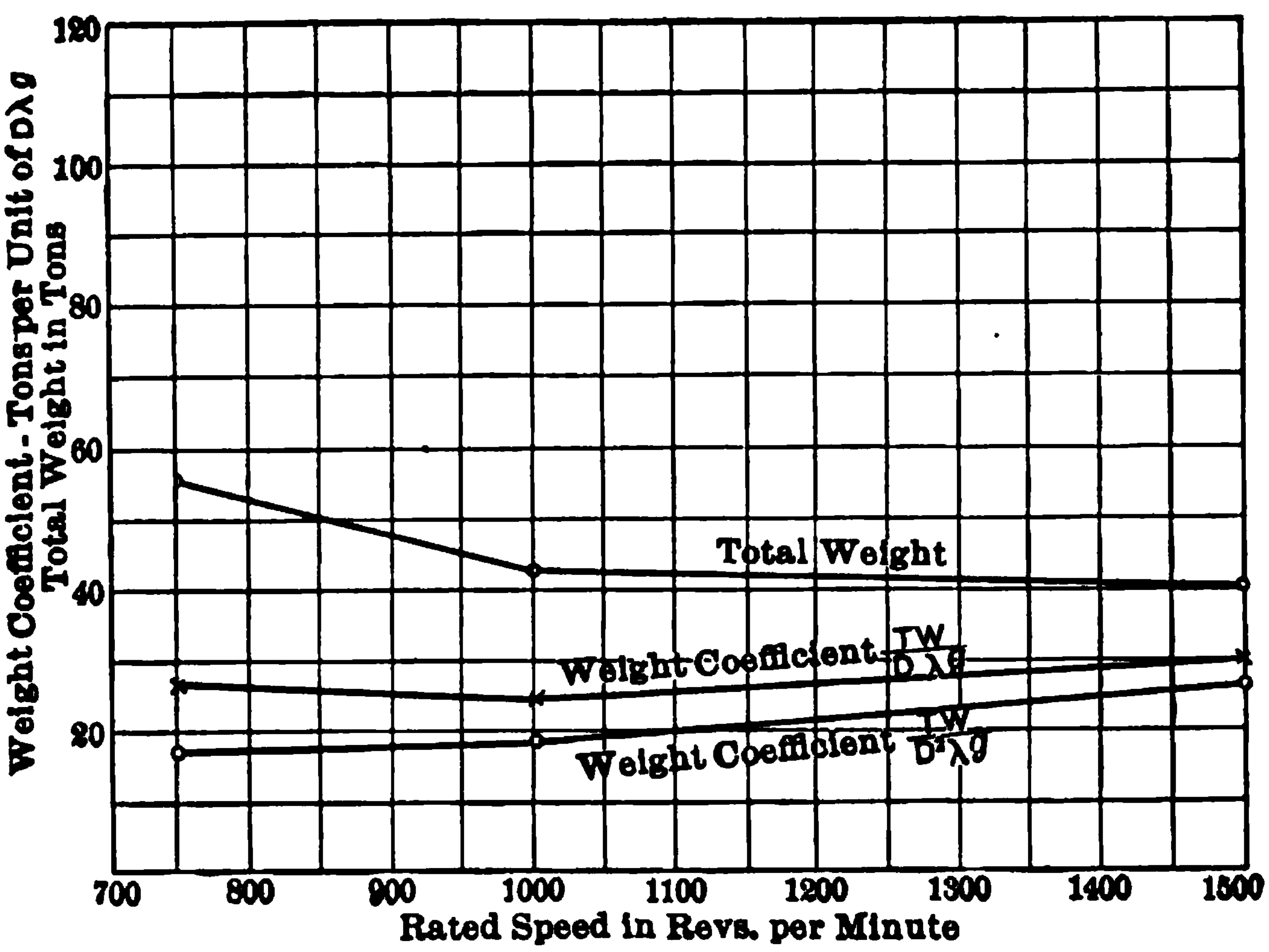


FIG. 111.— Total net weight and weight coefficient for 3000 kva. 50 cycle 3-phase alternator.

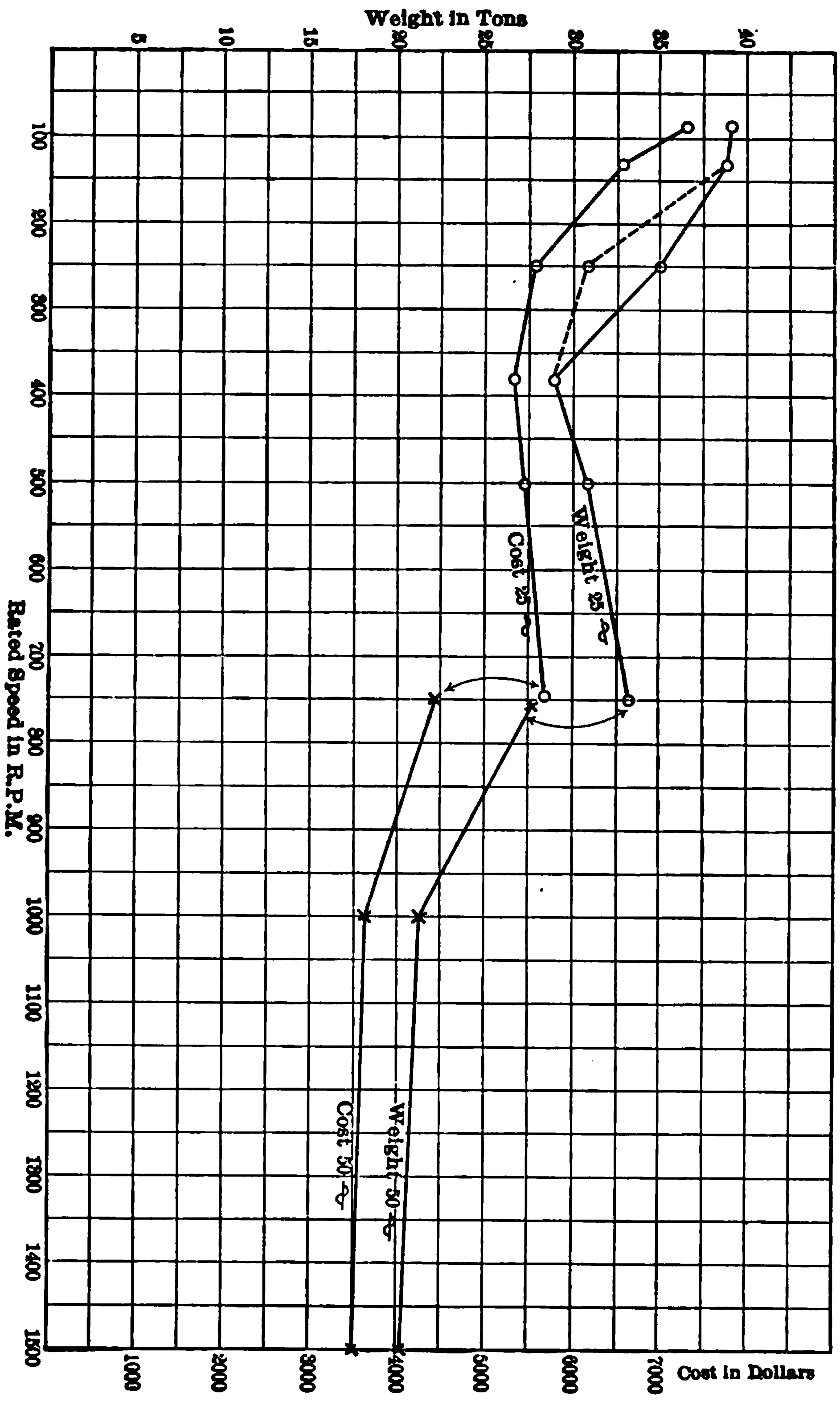


FIG. 112. — Curves showing weight and cost of effective materials of 3000 kva. 25 and 30 cycle 3-phase alternators for various rated speeds.

material is concerned, and, on account of its better electrical and mechanical properties, the 6-pole design for the slower speed would be preferable.

In Fig. 111 we have deduced the approximate total weight from an assumed value of the "weight factor." We have also plotted the two weight coefficients referred to earlier in the chapter, viz., the weight in tons per unit of $D\lambda g$ and of $D^2\lambda g$. In these designs the weight coefficient calculated on $D\lambda g$ is again more uniform than that calcu-

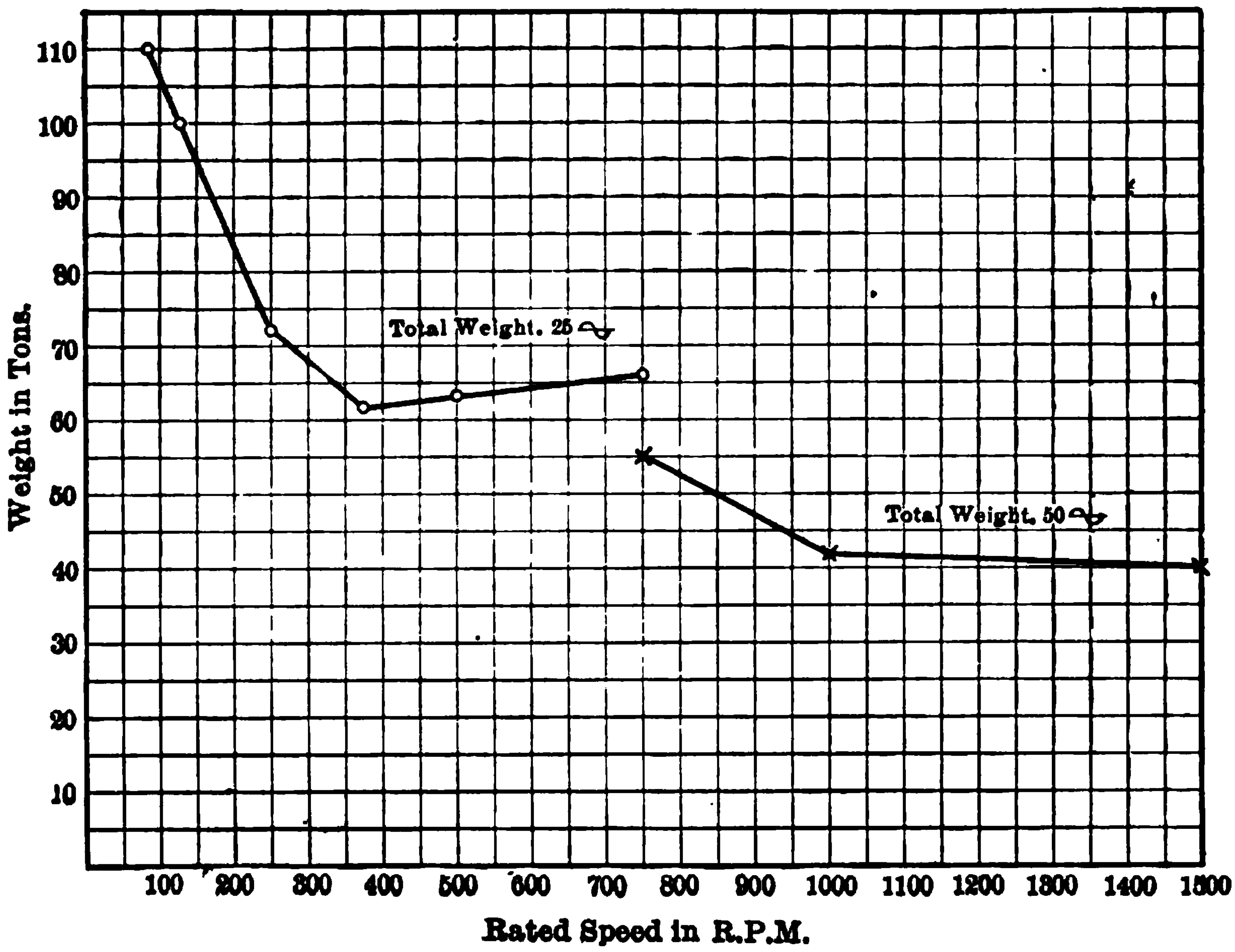


FIG. 113.— Curves showing total net weight of 3000 kva. 25 and 50 cycle 3-phase alternators for various rated speeds.

lated on $D^2\lambda g$, and its values are consistent with those obtained for the other group of machines in Fig. 102 (see also pages 18 to 25, Chapter II.)

In Fig. 112 there are reproduced together the curves of Figs. 101 and 110 showing the variation in the weight and cost of effective material. Each curve may be regarded as plotted progressively against the rated speed, although each is disjointed at the point where the designs change from 25 to 50 cycles, namely, at a speed of 750 R.P.M. The interesting feature is the tendency of each half of the curve, between 0 and 750 R.P.M. and between 750 and 1500 R.P.M.,

to exhibit a minimum point. This indicates clearly that the matter of economy of the design is not dependent solely on the rated speed, but also on the number of poles.

It will be noted that of the two 750 R.P.M. designs, that for 8 poles and 50 cycles is considerably more economical than the design for 4 poles and 25 cycles. The curves of the 25-cycle designs, when

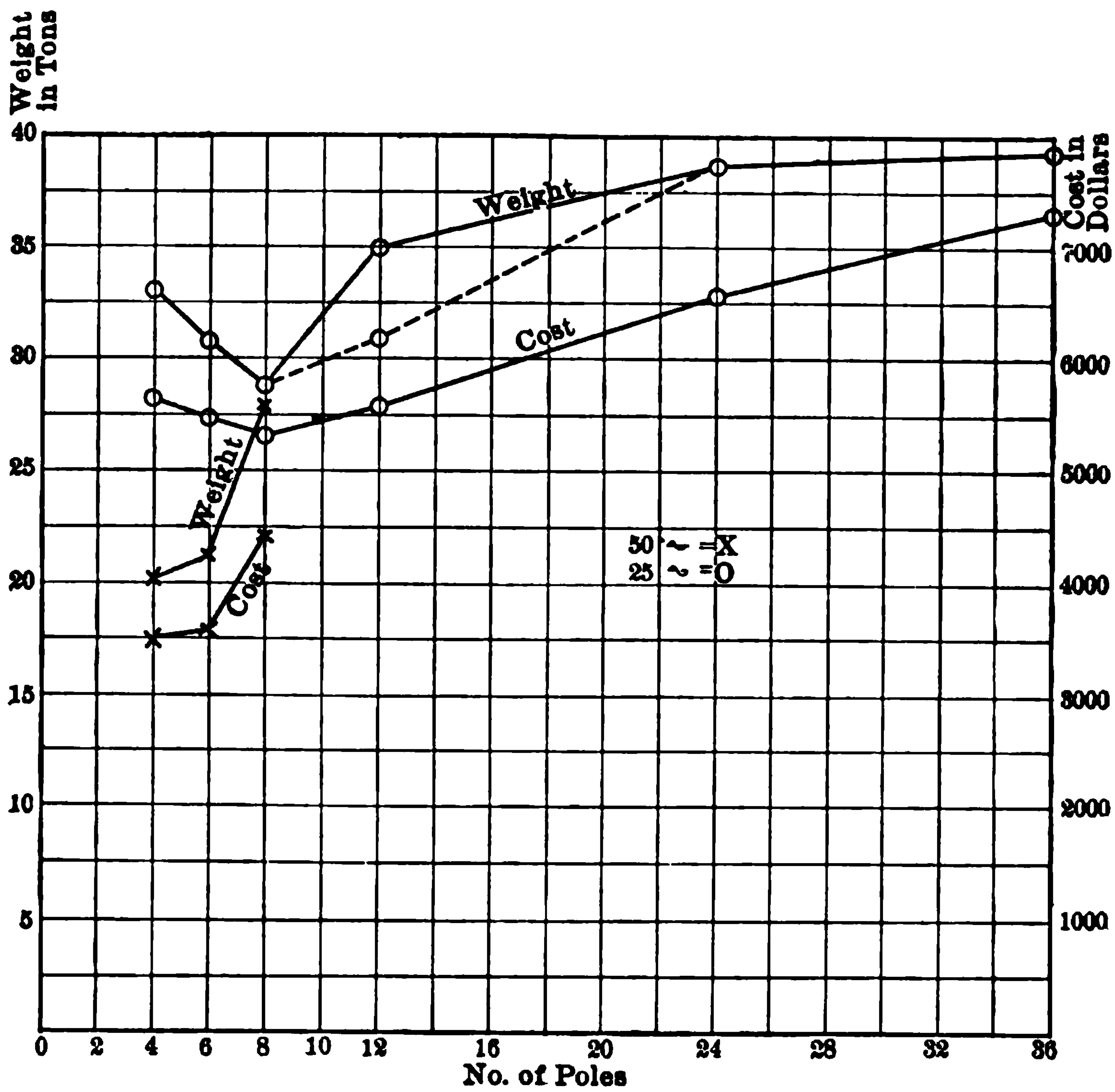


FIG. 114.—Weight and cost of effective material for 3000 kva. 25 and 50 cycle 3-phase alternator.

the speed has reached 750 R.P.M., have passed through the speed range corresponding to the greatest economy, and for 750 R.P.M. an 8-pole machine is found to be better than a 4-pole machine.

In Fig. 113 there is given a similar curve for the total net weight obtained from Figs. 102 and 111. The above remarks regarding Fig. 112 apply equally to Fig. 113.

It may be further noted that the total weight of a 2-pole

1500 R.P.M. 25-cycle design is found to be about 70 tons. The curve in Fig. 113 shows that a 4-pole 1500 R.P.M. design weighs only 40 tons. This large difference is simply due to the number of poles. To further illustrate this, we have in Figs. 114 and 115 plotted the

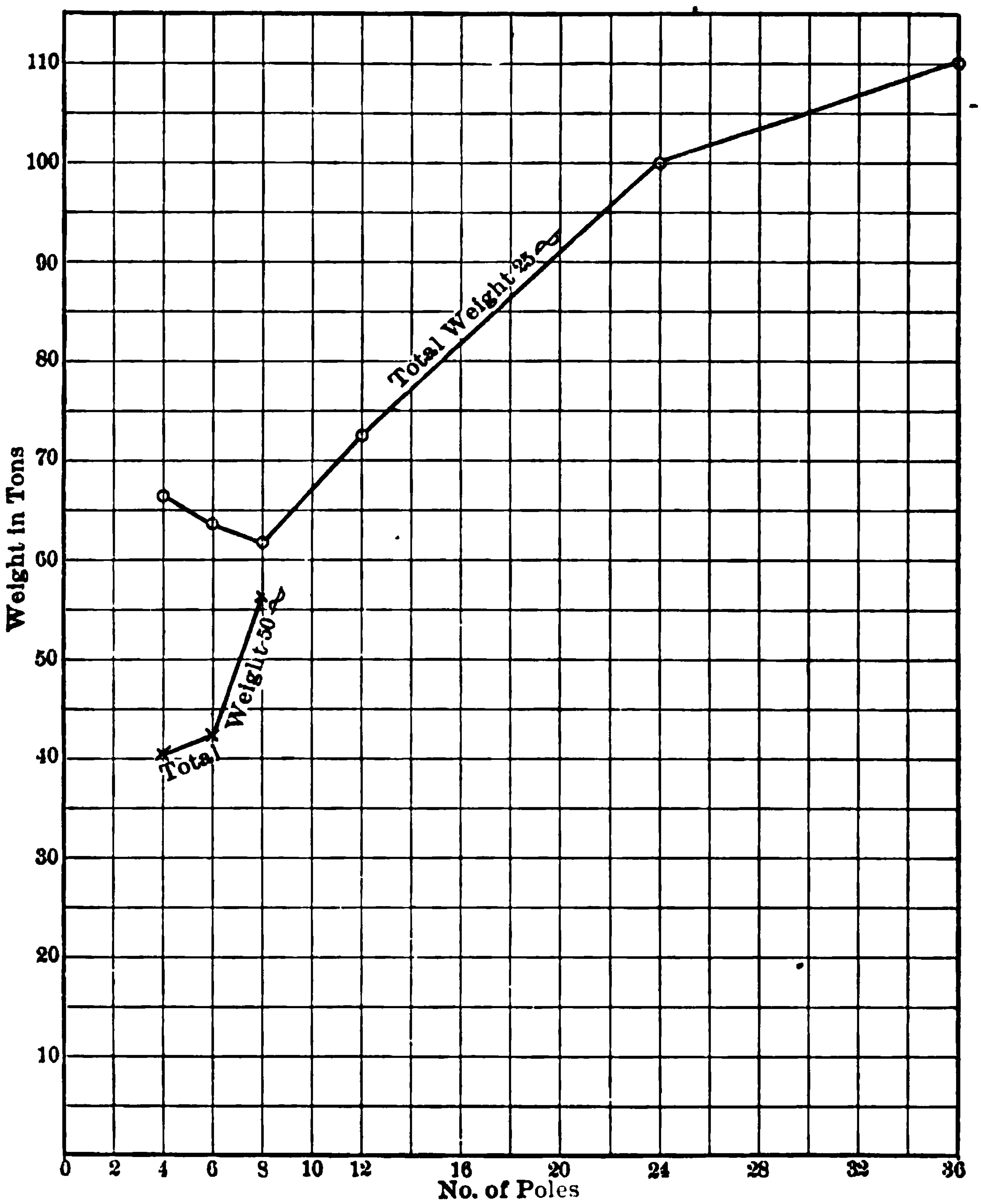


FIG. 115.— Total net weights for 3000 kva. 25 and 50 cycle 3-phase alternators.

weights and costs against the number of poles, the 25 cycle points being indicated by O and the 50 cycle by X.

These curves afford a direct comparison between machines having the same number of poles. A 2-pole design for 50 cycles must have

a speed of 3000 R.P.M. Such a design is practically uncommercial for a rating of 3000 kva., but were it attempted, the total weight would be found to be much greater than that of the 4-pole 1500 R.P.M. design. If such a point were obtained in extension of the 50 cycle curves in Fig. 113, the point of maximum economy would occur at the 4-pole design, and the curves would present a minimum point similar to that exhibited in the curves for the 25-cycle machines.

CHAPTER X.

HIGH SPEED DESIGNS FOR A RATED OUTPUT OF 6000 KVA. AND A STUDY OF LARGE DESIGNS IN GENERAL.

THE maximum rating for which a satisfactory design can be obtained at a given speed is governed chiefly by peripheral speed limitations. The highest peripheral speed which is permissible without encountering excessive stresses is of the order of 100 metres per second, although obviously somewhat lower peripheral speeds will be desirable with small diameters, since the centrifugal force at the periphery is, for a given peripheral speed, greater the smaller the diameter. If a design for a large rated output is required at a high speed, for instance, 6000 kva. at 750 R.P.M., it will be found that for a design of thoroughly good proportions, the diameter is such that the practical limit of peripheral speed is nearly reached.

It is almost impracticable to obtain a thoroughly satisfactory design for a higher rated output at that speed, or to obtain a satisfactory design for the same rated output at a higher speed. A good design for a larger rated output should preferably be obtained by employing a lower speed. Thus a good 10,000 kva. design would be almost impracticable at a speed of 750 R.P.M., and a speed of not over 500 R.P.M. would be called for. A design for 6000 kva. is not desirable at a speed much higher than 750 R.P.M. as we shall see by a study of the designs given in this chapter.

Were a very high speed associated with a larger rated output, it would be necessary to employ an armature of disproportionately great axial length. With such proportions the chief difficulty which would arise would relate to the absence of sufficient space for the requisite amount of field copper to secure good pressure regulation.

Below are given specifications for three 6000 kva. 750 R.P.M. alternators at three different frequencies and numbers of poles, namely,

Number of Poles	4	6	8
Frequency	25	37.5	50

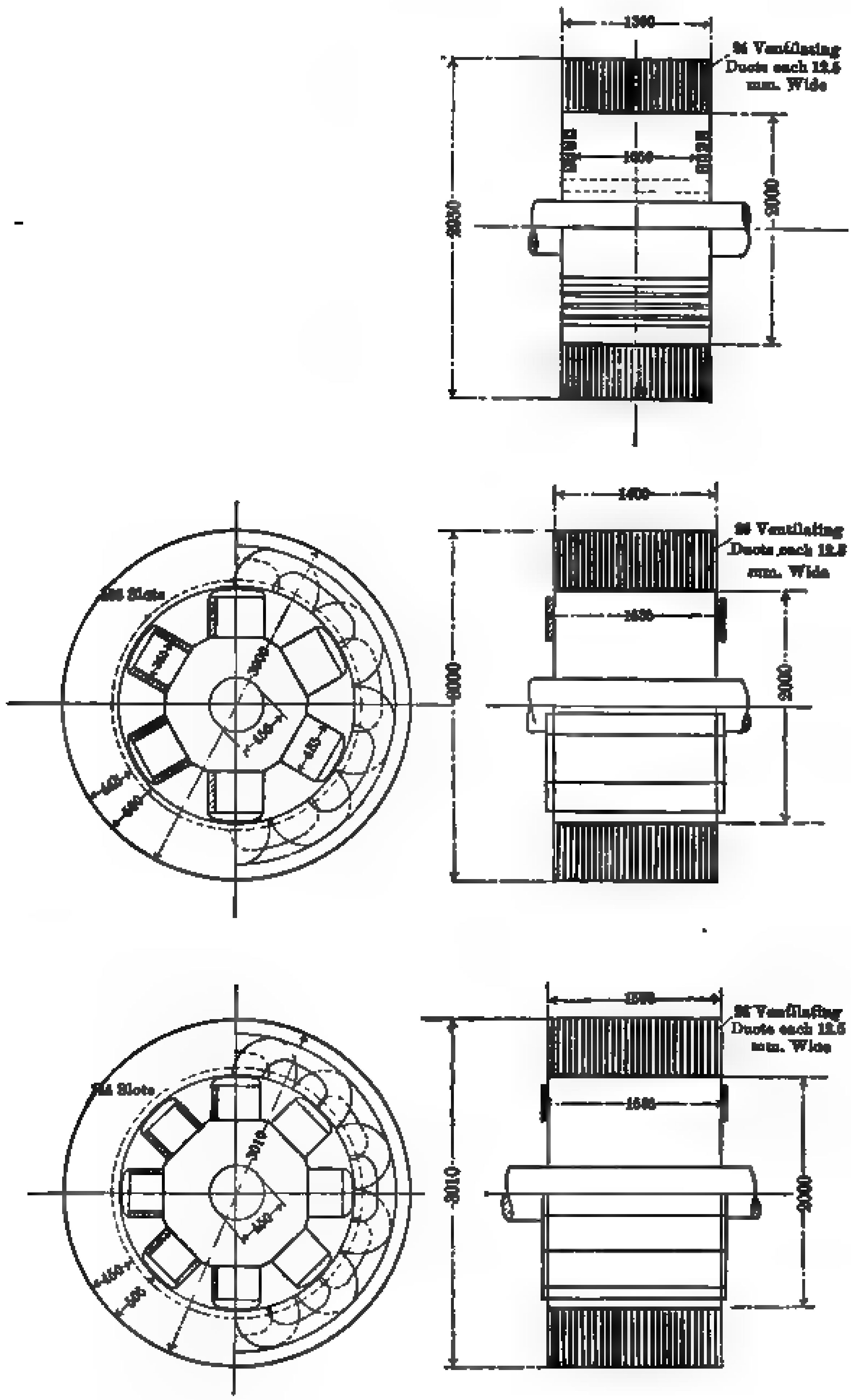


FIG. 116.— Outline drawings of 4, 6 and 8-pole, 750 R.P.M., 6000 kva. alternators.

These designs show that 750 R.P.M. is the highest desirable speed for 6000 kva. rated output. A good 6000 kva. design for 1000 R.P.M. and 50 cycles, with 6 poles, might be just as practicable, but

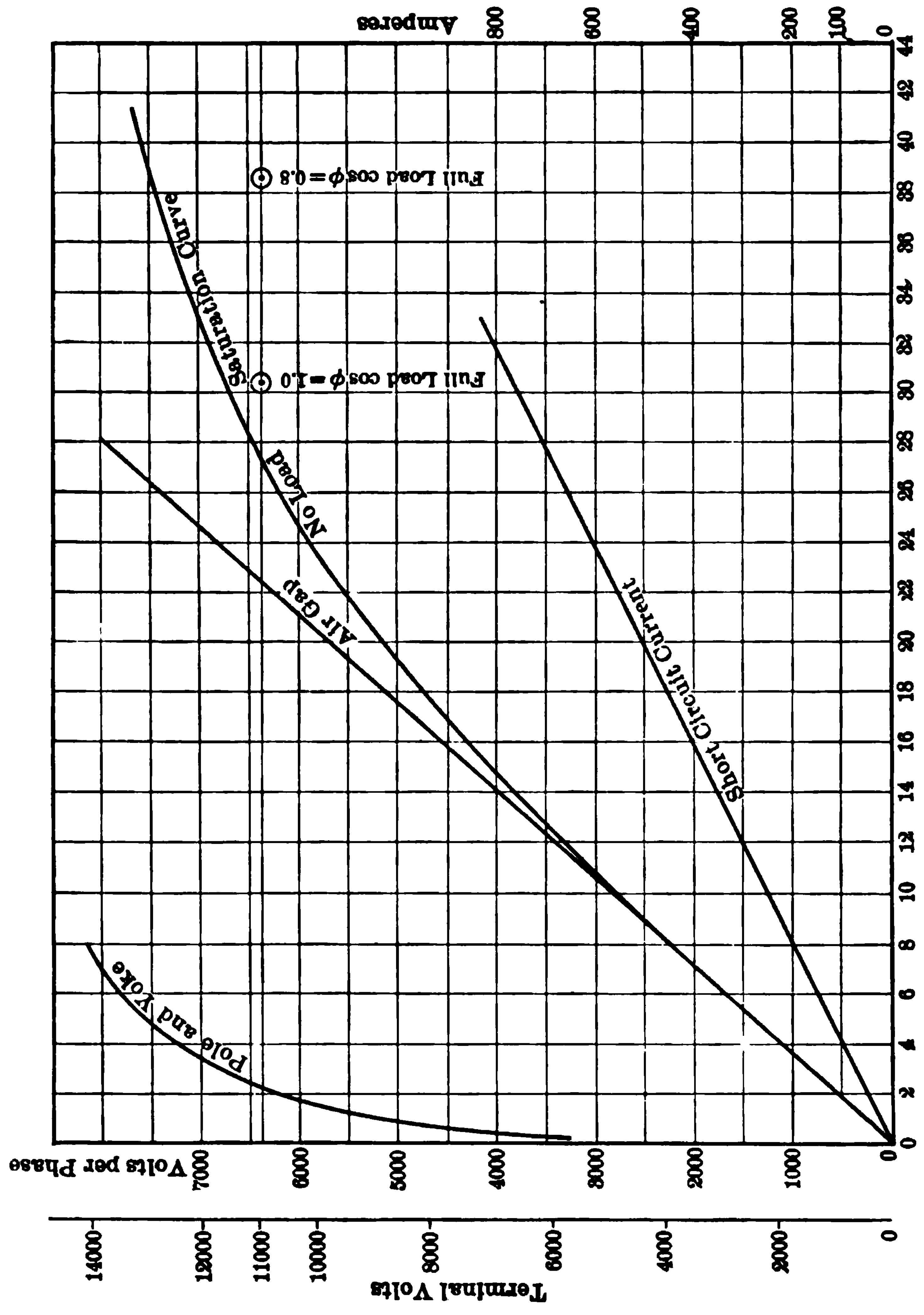


FIG. 117. — Saturation curve for 4-pole 750 R.P.M., 6000 kva. alternator.

one would encounter troublesome limitations as regards peripheral speed and the field winding space.

Outline drawings are given in Fig. 116. Saturation curves for these three designs are given in Figs. 117, 118 and 119 respectively.

It is seen from the specification for these designs that each is a

rational proposition, and that there is very little difference in the quality of the results between the 4, 6, and 8-pole designs. Were, however, a 2-pole design required for this speed and output, it would exhibit

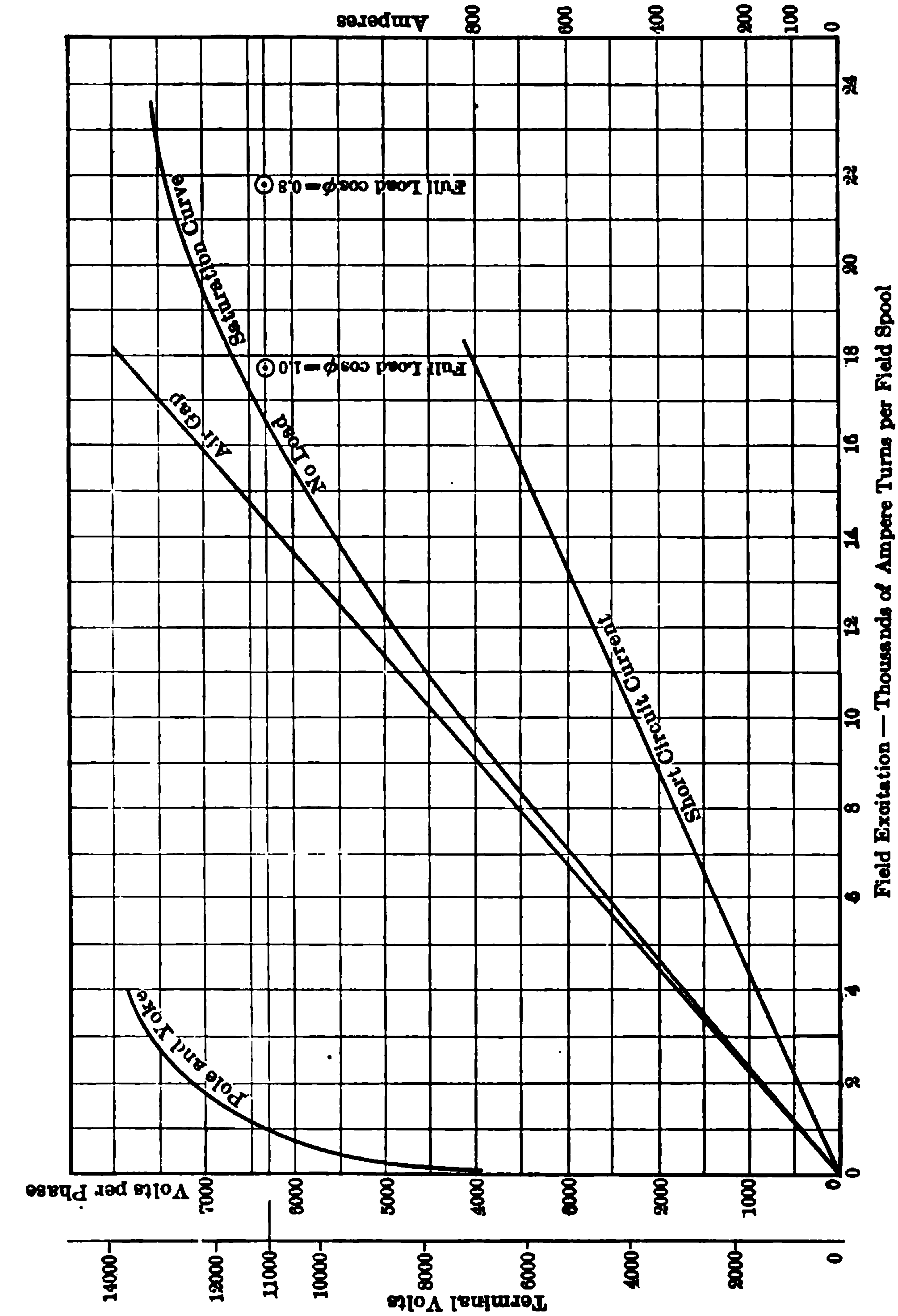


FIG. 118. — Saturation curve for 6-pole 750 R.P.M., 6000 kva. alternator.

all the bad characteristics of bi-polar designs which have been discussed in Chapters VIII and IX. The difficulties are further accentuated by the large rated output, and such a design is almost impracticable.

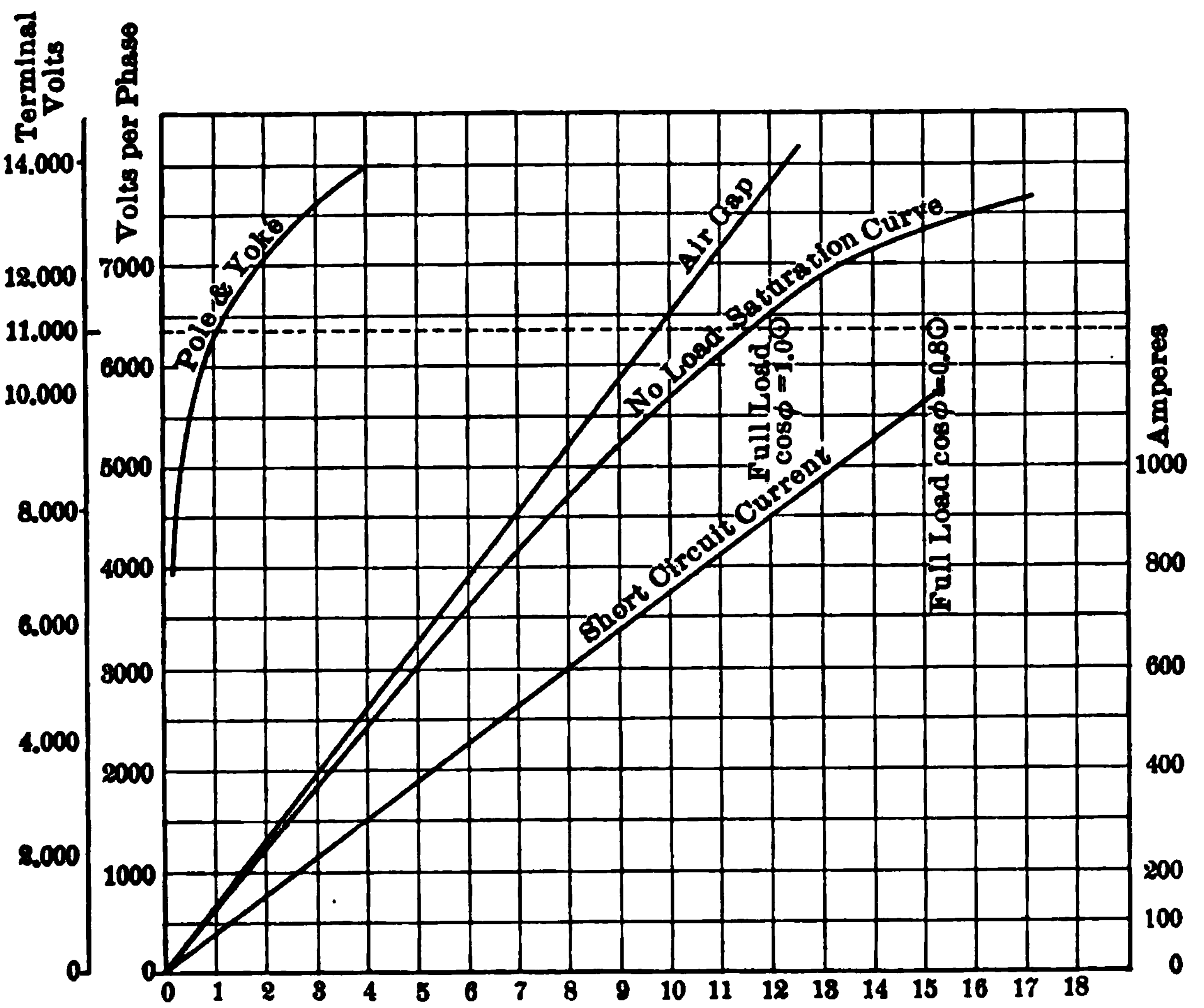


FIG. 119 —Saturation curve for 8-pole, 750 R.P.M., 6000 kva. alternator.

SPECIFICATION FOR

6000 kva. 3-Phase 4, 6, 8-Pole

750 R.P.M. 25, 37.5, 50 Cycles 11,000 Volt (Star Connected)

ALTERNATING CURRENT GENERATORS.

(All dimensions in cms.)

Output in kva.	6,000	6,000	6,000
Terminal voltage	11,000	11,000	11,000
Style of connection	Y	Y	Y
Current per terminal	315	315	315
Speed (R.P.M.)	750	750	750
Frequency	25	37.5	50
Number of poles	4	6	8
ARMATURE IRON			
Diameter at air gap	200	200	200
Diameter at bottom of slots	213	211	211
External diameter of laminations	295	300	301
Gross length between coreheads	130	140	150
Number of ventilating ducts	24	26	28
Width of each duct	1.25	1.25	1.25
Percentage insulation between stampings	10%	10%	10%
Effective core length (iron)	90	97	105
Width of the end ducts	1.25	1.25	1.25
Pole pitch at armature face	157	105	78.5

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

Slots and Teeth			
Total number of slots	96	108	144
Type of slot	Open	Open	Open
Slot pitch at armature face	6.55	5.82	4.36
Width of slot	3.55	2.82	1.88
Width of slot opening	3.55	2.82	1.88
Width of tooth at armature face	3.0	3.0	2.48
Width of tooth at narrowest part	2.6	2.6	2.08
Radial depth of slot	6.5	5.5	5.5
Armature Copper			
Number of slots per pole per phase	8	6	6
Number of conductors per slot	4	3	2
Section of conductor (true)	1.58	1.3	1.05
Current	315	315	315
Current density — amperes per sq. cm.	200	242	300
Dimensions of conductor bare	1.02×2.08	1.06×1.64	0.7×2.0
Thickness of slot insulation	0.55	0.55	0.55
Number of turns in series per phase	64	54	48
Weight of copper per phase	733	407	258
Total weight of armature copper (metric tons)	2.2	1.22	0.775
Rotor Iron			
Diameter at pole face	192	195.3	197
Length of air gap	4	2.35	1.5
Pole pitch at pole face	151	102	77.4
Circumferential length of pole arc	93	62.8	47.2
Ratio pole arc / pole pitch	0.6	0.6	0.6
Gross axial length (parallel to shaft)	130	140	150
Effective axial length of pole (iron)	130	140	150
Breadth of pole body across shaft	60	35	32.2
Field Copper			
Total length of winding space	6×4	24	24
Number of turns per pole	50×4	110	75
Nature of winding	Strip wound flat	Strip wound on edge	Strip wound on edge
Width of strip	5.5	6.0	3.5
Thickness of strip	2.07	1.67	2.7
Insulation on bobbin walls	0.25	0.25	0.25
Thickness of bobbin cheeks	0.25	0.25	0.25
Total cross section of winding space per pole	306	153	92
Total cross section of copper	228	110	71
Space factor	0.745	0.72	0.77
Current at full load and $\cos\phi = 0.8$	193	200	203
Current density amperes per sq. cm.	169	200	215
Resistance of all field spools in series at 60° C.	0.532	0.502	0.49
Volts across field at above amperes	103	100	99.5
Exciter voltage	125	125	125
Weight of copper per spool (metric tons)	0.77	0.372	0.243
Total weight of copper on all poles (metric tons)	3.08	2.23	1.94

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

0

5

4
1

8
4
5
2
5

0
0
0
0
0

Total	27,300	39,510	17,570	26,750	12,450	18,700
Iron $A.T.$ in per cent of total	18	32	18.7	36	21.7	37
Gap $A.T.$ in per cent of total	82	68	81.3	64	78.3	63

Losses			
Armature Copper			
Mean length of turn . . . metres	8.1	6 5	5.75
Resistance per phase at 60° C. . . .	0.0655	0.054	0.05
Total C^2R loss at full load cos $\phi = 1$ kw.	19.5	16.05	15
Armature Iron			
Weight of armature iron (excluding teeth) . . . . tons	22.75	26.5	28.9
Frequency	25	37.5	50
Flux density — kilolines per sq. cm.	11.5	7 75	6.0
Kw. per ton	5.3	4.7	4
Total core loss . . . . kw.	120	124	116
Teeth			
Weight of teeth . . . . tons	1.31	1.35	1.42
Flux density — kilolines per sq. cm.	20 0	18.7	18.0
Kw. per ton	16	27	35
Total tooth loss . . . . kw.	21 0	36.5	50
Total iron loss	141	160.5	166
Iron loss + copper loss (cos $\phi = 1$) kw.	160.5	176.5	181
Field Copper			
Excitation power at full load cos $\phi = 1$ kw.	12 3	12.8	13
Excitation power at full load (cos $\phi = 0.8$) . . . . . kw.	19.8	20.0	20 2
Watts per sq. dcm. of external surface of field spool (cos $\phi = 0.8$) . . .	48	34	26
Armature Heating Coefficients			
Watts per sq. dcm. of armature surface cos $\phi = 1$			
Calculated on $\pi(D\lambda g + 0.7\tau)$	123	123	120

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

Losses — *continued*			
Efficiency			
Armature copper loss . . . . kw.	19.5	16.05	15
Armature iron loss . . . . . kw.	141	160.5	166
Field copper loss . . . . . . kw.	12.3	12.8	13
Total electrical losses . . . . kw.	172.8	189.4	194
Efficiency (excluding friction losses) — full load ($\cos\phi = 1$)	97%	97%	97%
Efficiency (excluding friction losses) — half load	. . .	. . .	95%
Inherent regulation at full load ($pf = 1$)	4.25%	3.6%	3.5%
At full kva. and power factor = 0.8 .	16%	16%	16%
Constants and Coefficients			
Weight of effective material (M. Tons)	48	52	57
Weight of effective material per kva. . . . . (kg.)	8	8.7	9.5
Cost of effective material . . . . $	8,650	8,200	8,500
Cost of effective material per kva . $	1.44	1.37	1.42
$D \times \lambda g$ . . . . . . ($D\lambda g$ in metres)	2.6	2.8	3.0
$D^2\lambda g$ metres . . . (" " ")	5.2	5.6	6.0
Ratio $\lambda g/\tau$	0.83	1.33	1.91
Ratio $D/\lambda g$	1.54	1.43	1.33
Output coefficient . . . . ξ	1.54	1.43	1.33
Ampere conductors per cm. of periphery . . . . a	193	163	144
Flux (no load) per sq. cm. of armature surface . . . . β	4,160	4,570	4,850
Peripheral speed (metres per sec.) v	75.5	76.5	77.5
Watts per c. cm. of active belt . . .	10.9	12.0	11.2
Ratio of (no load) field ampere turns to armature ampere turns	1.8	1.94	2.02
Ratio of short circuit to full load current	2.2	2.4	2.75
Kva. per pole	1,500	1,000	750
Estimated total net weight ($T.W.$) tons	96	104	114
Weight coefficient $T.W./D\lambda g$ . . .	37	37.2	38.4
Weight coefficient $T.W./D^2\lambda g$ . . .	18.5	18.6	19.2

Weights and Costs.

	Weights (Metric Tons).			Costs ($).		
Number of Poles.	4	6	8	4	6	8
						2
Magnet cores	6.72	8.05	7.86	840	1012	985
Magnet shoes	4.14	2.05	2.36	518	256	295
Magnet yoke	7.7	10.0	13.4	962	1250	1675
Armature laminations	24.2	28.22	31.03	3027	3527	381
Effective iron (total)	42.76	48.32	54.65	5347	6045	6767
Armature copper	2.2	1.22	0.78	1375	762	485
Field copper	3.08	2.23	1.94	1925	1385	1212
Effective copper (total)	5.28	3.45	2.72	3300	2147	1697
Total effective material	48.04	51.75	57.37	8647	8192	8464

We shall, at a later stage, use the 8-pole 50-cycle design for comparison with designs of a smaller rated output, but of the same speed and frequency. For the present we may make a few comparisons between these three designs showing the influence of the frequency at a given speed.

In these designs, a constant air gap diameter (D) of 200 centimetres has been taken, as the peripheral speed with this diameter is nearly 80 metres per second. In accordance with the general principles which we have followed, we should have chosen larger diameters for

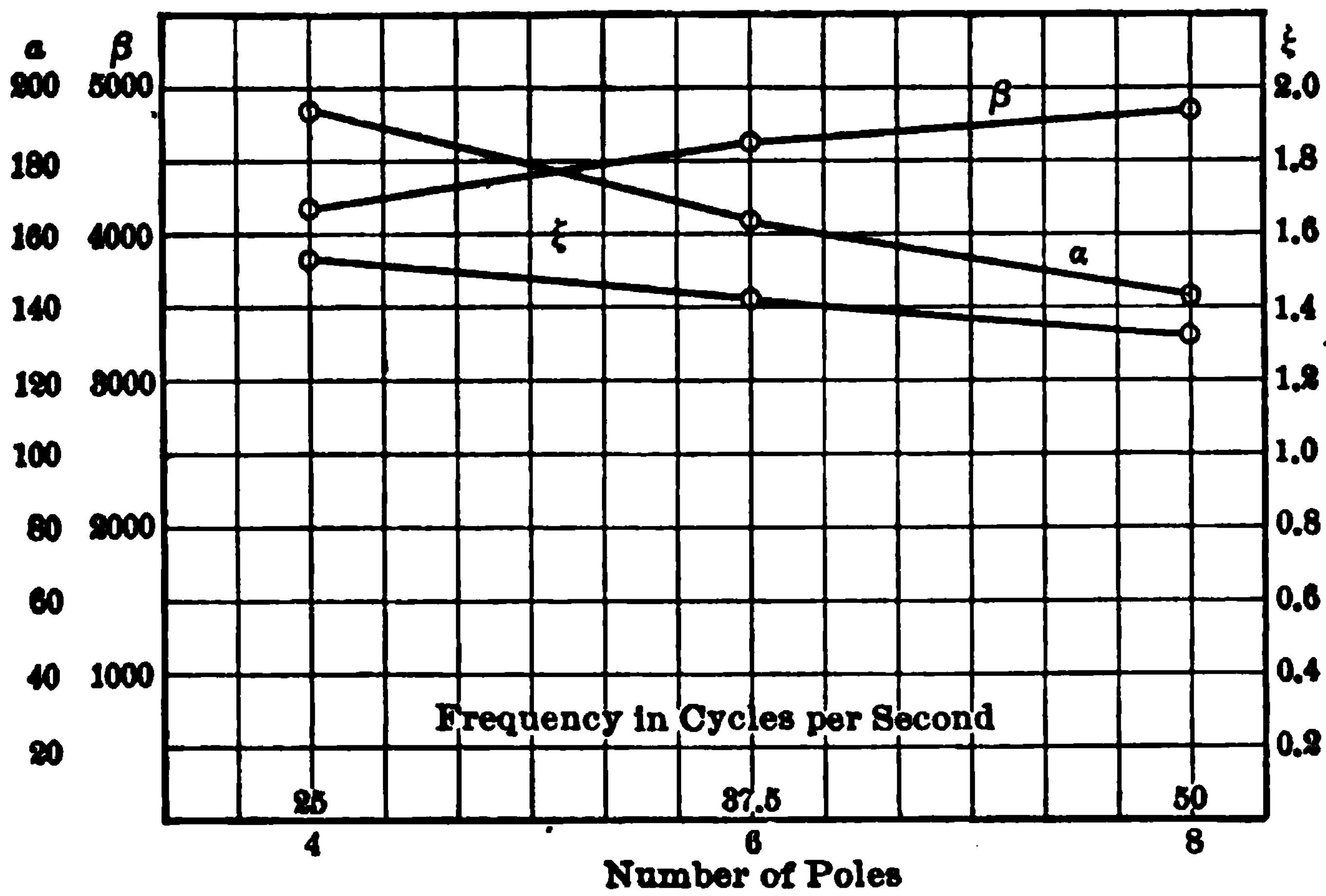


FIG. 120.—α, β and ξ for 6000 kva. designs.

the machines of higher frequency and with more poles. As it is, the 4-pole design has twice the pole pitch of the 8-pole, and consequently the ratio $\frac{\lambda g}{\tau}$ in the former case is twice that in the latter.

Had a considerably larger diameter been employed for the 8-pole design, the weights of magnet yoke, field and armature copper would have been reduced to a certain extent, and thus also the total net weight. As it is, however, the 8-pole machine is rather heavier than the 4-pole, which is a consequence of the above procedure, together with the fact that in the weight of the magnet yoke all the material from the pole seat to the shaft has been included as effective iron, whereas the actual amount required to carry the magnetic flux

would be considerably less. The higher the frequency the lower is the value obtained for the output coefficient. This is in accordance with the conclusions arrived at in Chapter II. Hence the values of $D\lambda g$ and $D^2\lambda g$ are rather higher as the number of poles increases.

The variations in the components α and β, and of the output

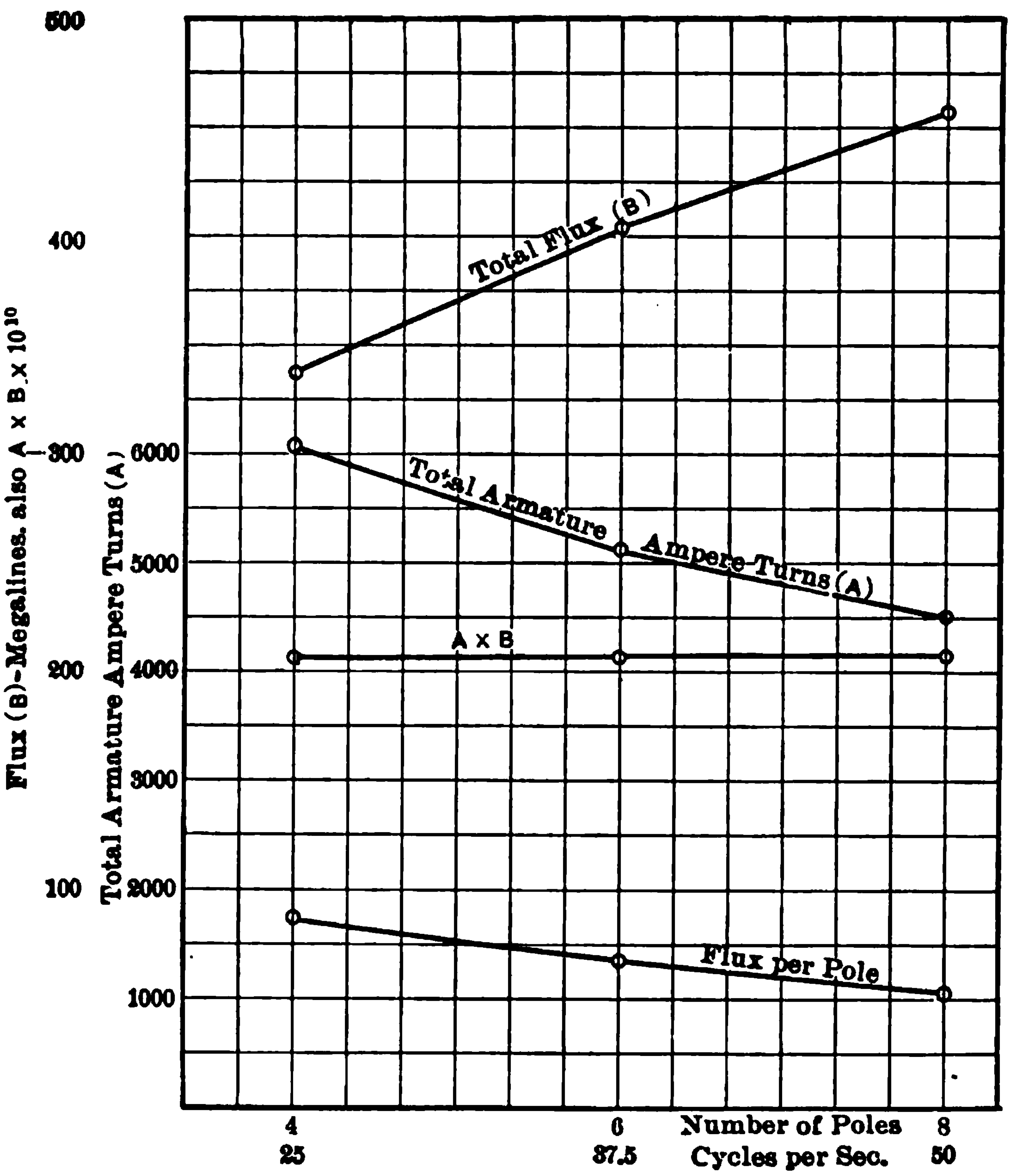


FIG. 121.—Curves of flux and armature strength in 6000 kva. designs.

coefficient, are shown by the curves in Fig. 120. While ξ slightly decreases, α decreases at a more rapid rate and β increases with the frequency. This is on account of the fact that the armature reaction is greater the higher the frequency of the design, and the armature must consequently not be so strong in a high frequency design.

On the score of losses and efficiency there is nothing to choose. Although the 8-pole machine has one half the pole pitch of the 4-pole, the radial depth of the armature laminations is about the same in both cases, the proportions being controlled by considerations of core loss and heating. The weight of armature laminations is greater in the 8-pole designs.

Figs. 121 and 122 relate to the flux and armature and field strength.

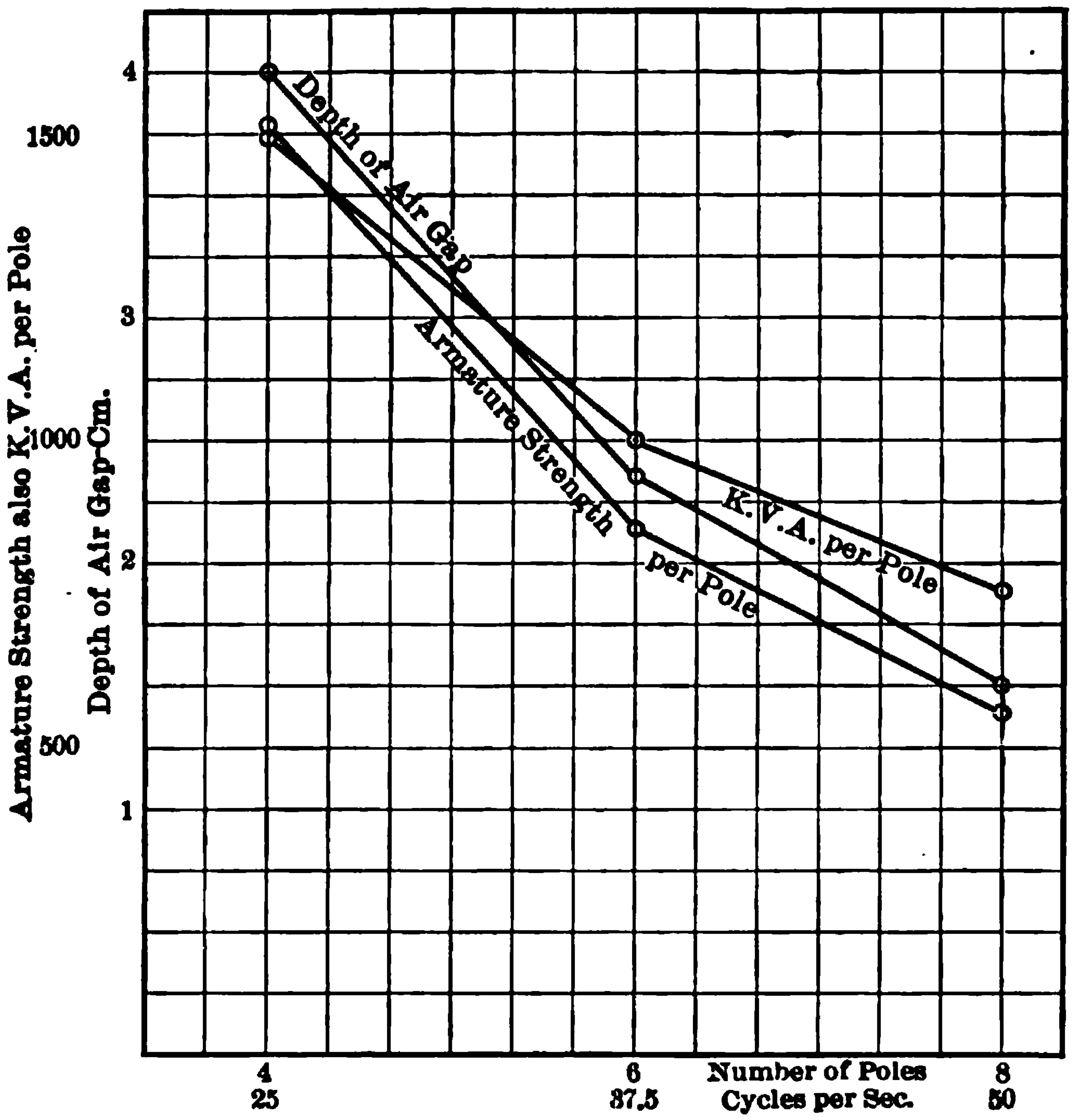


FIG. 122.— Curves for armature strength and depth of air gap for 6000 kva. designs.

The flux per pole is nearly inversely proportional to the number of poles. As β and $D\lambda g$ increase with the number of poles, the total flux crossing the gap must also increase with the number of poles. In these figures there are also plotted the total armature ampere turns and the product (total flux $\times$ total armature ampere turns). This product is a constant in accordance with equation (4) on page 8, Chapter II.

In Fig. 122 are plotted the armature strength, the kva. per pole, and the radial depth of air gap. The armature strength is practically inversely proportional to the number of poles, and the depth of air gap is almost proportional to the armature strength. This latter circumstance is due to the fact that the three designs each have the same degree of saturation. The dimensions of the

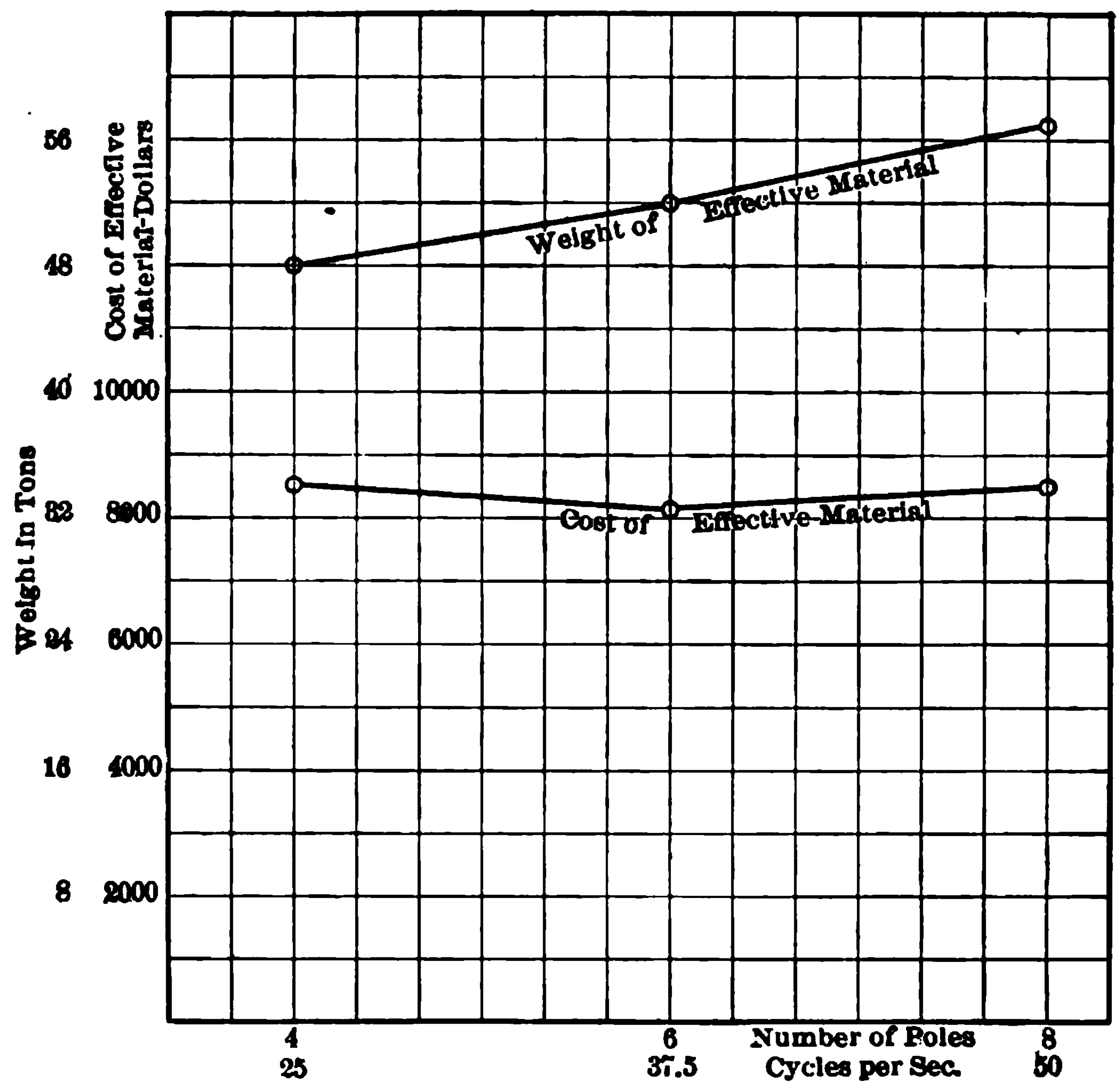


Fig. 123. — Curves for weight and cost of effective material for 6000 kva. alternators.

magnetic paths are great in each case so that the air gap ampere turns in per cent of the total field ampere turns per pole are about 80 per cent in each case. The ratio of field to armature ampere turns is 1.7, 1.9, and 2.0 for the 4, 6, and 8-pole designs respectively.

In Fig. 123 are plotted the weight and cost of effective material, and in Fig. 124 the total weight and the weight coefficients, in terms of $D\lambda g$ and $D^2\lambda g$. In this case the two coefficients are proportional, as a consequence of the use of a constant value for D, the diameter at the air gap.

Let us now undertake a comparison of three designs for the same speed, frequency, and number of poles, but of different rated outputs. For this purpose we have taken the following cases:

A. 1500 kva. 750 R.P.M. 50 cycle 8 poles.
B. 3000 kva. 750 R.P.M. 50 cycle 8 poles.
C. 6000 kva. 750 R.P.M. 50 cycle 8 poles.

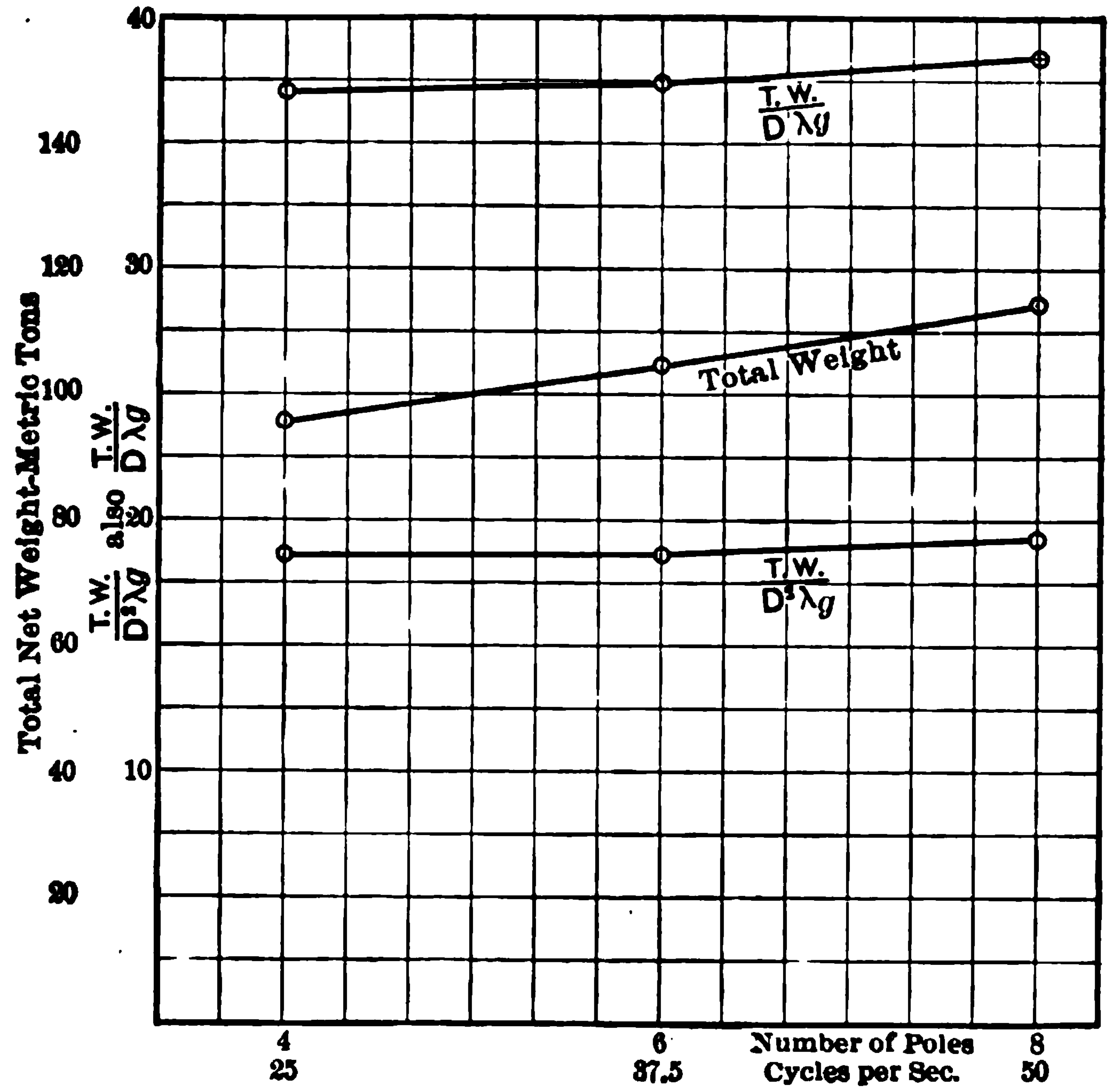
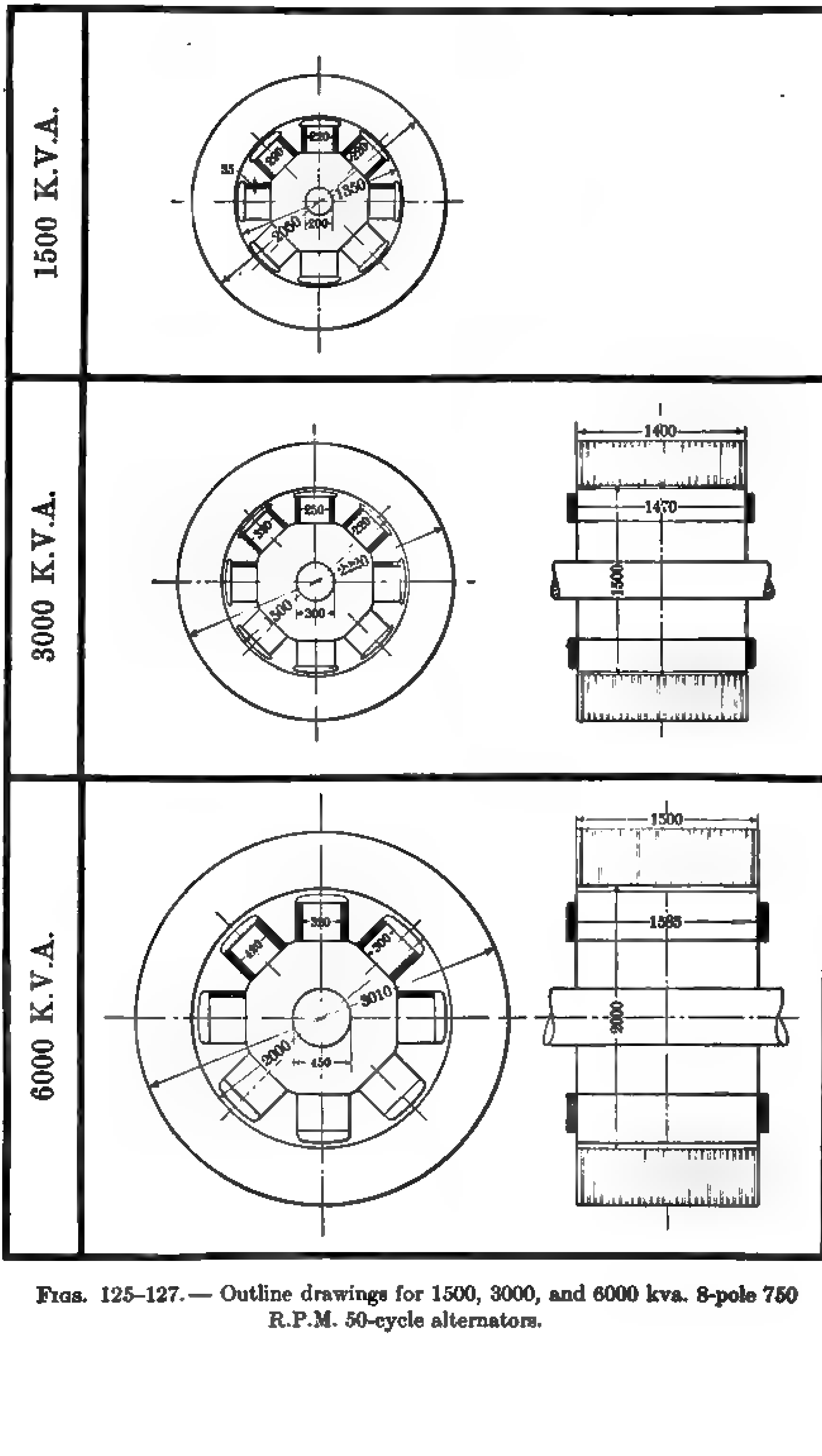


FIG. 124.— Curves for the total weight and for the weight coefficients for 6000 kva. alternators.

Design B has already been obtained in Chapter IX, see page 184.
Design C has just been given on pages 201 to 204 of this chapter.
The specification for design A is given below, and designs B and C have been brought in parallel vertical columns.

In Figs. 125, 126 and 127, outline drawings of these machines are shown together.



FIGS. 125–127.— Outline drawings for 1500, 3000, and 6000 kva. 8-pole 750 R.P.M. 50-cycle alternators.

SPECIFICATION FOR

1500, 3000, 6000 kva. 3-Phase 8-Pole

750 R.P.M. 50 Cycles 11,000 Volt

ALTERNATING CURRENT GENERATORS.

(All dimensions in cms.)

	A.	B.	C.
Output in kva.	1,500	3,000	6,000
Terminal voltage	11,000	11,000	11,000
Style of connection	Y	Y	Y
Current per terminal	79	158	315
Speed R.P.M.	750	750	750
Frequency	50	50	50
Number of poles	8	8	8
Armature Iron			
Diameter at air gap . . . D	135	150	200
Diameter at bottom of slots	145	162	211
External diameter of laminations	205	222	301
Gross length between coreheads . . . λg	85	140	150
Number of ventilating ducts	14	28	28
Width of each duct	1.25	1.25	1.25
Percentage insulation between stampings	10%	10%	10%
Effective core length (iron)	60	95	105
Width of the end ducts	1.25	1.25	1.25
Pole pitch at armature face	53	59	78.5
Slots and Teeth			
Total number of slots	96	120	144
Type of slot	Open	Open	Open
Slot pitch at armature face	4.42	3.9	4.36
Width of slot	2.2	2.1	1.88
Width of slot opening	2.2	2.1	1.88
Width of tooth at armature face	2.22	1.83	2.48
Width of tooth at narrowest part	...	...	2.08
Radial depth of slot	5	6	5.5
Armature Copper			
Number of slots per pole per phase	4	5	6
Number of conductors per slot	8	4	2
Section of conductor (true)	0.28	0.65	1.05
Current	79	158	315
Current density — amps. per sq. cm.	282	240	300
Dimensions of conductor bare	(0.5×0.75)	2×(0.45×0.965)	0.7×2
Thickness of slot insulation	0.45	0.5	0.55
Number of turns in series per phase	128	80	48
Weight of copper per phase (tons)	0.12	0.24	0.26
Total weight of armature copper (tons)	0.36	0.72	0.78

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS—*Continued.*

	A.	B.	C.
Rotor Iron			
Diameter at pole face	132	147	197
Length of air gap	1.5	1.5	1.5
Pole pitch at pole face	53	59	77.4
Circumferential length of pole arc	35.3	39	47.2
Ratio pole arc / pole pitch	0.66	0.66	0.6
Gross axial length (parallel to shaft)	85	140	150
Effective axial length of pole (iron)	85	126	150
Breadth of pole body across shaft	22	25	32.2
Field Copper			
Total length of winding space	22	22	24
Number of turns per pole	84	50	75
Width of strip	3.015	3	3.5
Thickness of strip	0.20	0.31	0.27
Total cross section of winding space per pole	66.5	66	92
Total cross section of copper	50.1	45	71
Space factor	0.755	0.68	0.77
Current at full load and cos $\phi=0.8$	146	260	203
Current density amperes per sq. cm.	242	280	215
Resistance of all field spools in series at 60° C.	0.646	0.303	0.49
Volts across field at above amps.	95	75	100
Exciter voltage	125	100	125
Weight of copper per spool (metric tons)	.103	.146	24.3
Total weight of copper on all poles (metric tons)	.830	1.170	1.940
Magnetic Data volts per phase			
Armature flux per pole (normal volts) (megalines)	23	34	57
Leakage coefficient (calculated at no load)	1.15	1.14	1.14
Flux in the pole core	26.5	38.7	65
Magnetic Densities in kilolines per sq. cm.			
Armature	6.4	6.0	6.5
Teeth (corrected)	18.8	18.0	18.0
Pole core	14.2	15.3	15.5
Yoke	...	...	...
At pole face	7.2	6.8	8.1
Ampere Turns			
Armature	100	80	100
Teeth	400	540	600
Gap	8,650	8,100	9,750
Pole core	330	500	1,000
Yoke	...	...	...
Total no load ampere turns	9,500	9,200	11,450
Iron ampere turns in per cent of total	10%	12%	14%
Gap ampere turns in per cent of total	90%	88%	86%

SPECIFICATION FOR ALTERNATING CURRENT GENERATORS — *Continued.*

	A.	B.	C.
EXCITATION			
Excitation power at full load $\cos \phi = 1$ kw.	9.3	15.7	13
Excitation power at full load $\cos \phi = 0.8$ kw.	13.3	19.5	20.2
Watts per sq. dcm. of external surface of field spool ($\cos \phi = 0.8$)	25	31	26.5
ARMATURE HEATING COEFFICIENTS			
Watts per sq. dcm. of armature surface $\cos \phi = 1$			
(A) Calculated on $\pi (D\lambda g + 0.7\tau)$	120	117	139
(B) Calculated on $\pi D \lambda g$	114	144	195
(C) Calculated on total surface	62	48	57.5
EFFICIENCY (excluding friction and windage)			
Armature copper loss kw.	6.6	9.4	15
Armature iron loss kw.	56	90	166
Field copper loss kw.	9.3	5.7	13
Total electric and magnetic losses kw.	71.9	115	194
Efficiency — full load $\cos \phi = 1$	95.5%	96.5%	97%
Efficiency half load	92.0%	93.8%	94.5%
Inherent regulation at full load $\cos \phi = 1$	5%	7.5%	4%
At full kva. and $\cos \phi = 0.8$	17%	17.5%	16%
CONSTANTS AND COEFFICIENTS			
Weight of effective material (metric tons)	15.35	23.5	57.4
Weight of effective material per kva. (kg).	0.102	0.0785	0.096
Cost of effective material	2,500	4,400	8,400
Cost of effective material per kva.	6.65	4.75	5.65
$D \times \lambda g$	1.15	2.1	3.0
$D^2 \lambda g$	1.55	3.15	6.0
Ratio $\frac{\lambda g}{\tau}$	1.605	2.38	1.94
Ratio $\frac{D}{\lambda g}$	1.59	1.07	1.33
Output coefficient ξ	1.3	1.27	1.33
Ampere conductors per cm. of periphery α	1.41	1.60	1.44
Flux (no load) per sq. cm. of armature surface β	5,110	4,130	4,850
Peripheral speed (metres per sec.) v	53	59	78.6
Watts per c. cm. of active belt	8.0	7.5	11.2
Ratio of (no load) field ampere turns to armature ampere turns	2.5	2.0	2.0
Ratio of short circuit to full load current	3.2	2.54	2.75
kva. per pole	188	375	750
Estimated total net weight tons	31.0	56.0	115.0
Weight coefficient $\frac{\text{Total net weight}}{D\lambda g}$	24.0	26.6	38.4
Weight coefficient $\frac{\text{Total net weight}}{D^2\lambda g}$	20.0	17.8	19.2

WEIGHTS AND COSTS.

	Weights — Tons.			Costs — Dollars.		
	A.	B.	C.	A.	B.	C.
Magnet cores	2.57	3.46	7.86	320	430	980
Magnet shoes	0.51	1.26	2.36	60	160	290
Magnet yoke	3.38	7.13	13.4	420	890	1680
Armature laminations	7.7	14.47	31.03	960	1810	3820
Effective iron (total)	14.2	26.32	54.65	1760	3290	6770
Armature copper	0.36	0.72	0.78	280	450	480
Field copper	0.83	1.17	1.94	520	690	1210
Effective copper (total)	1.19	1.89	2.72	800	1140	1690
Total effective material	15.4	28.2	57.4	2500	4400	8400

The 1500 kva. rating may be favourably designed for a speed of 1000 R.P.M. or 1500 R.P.M., as may also the 3000 kva. design,

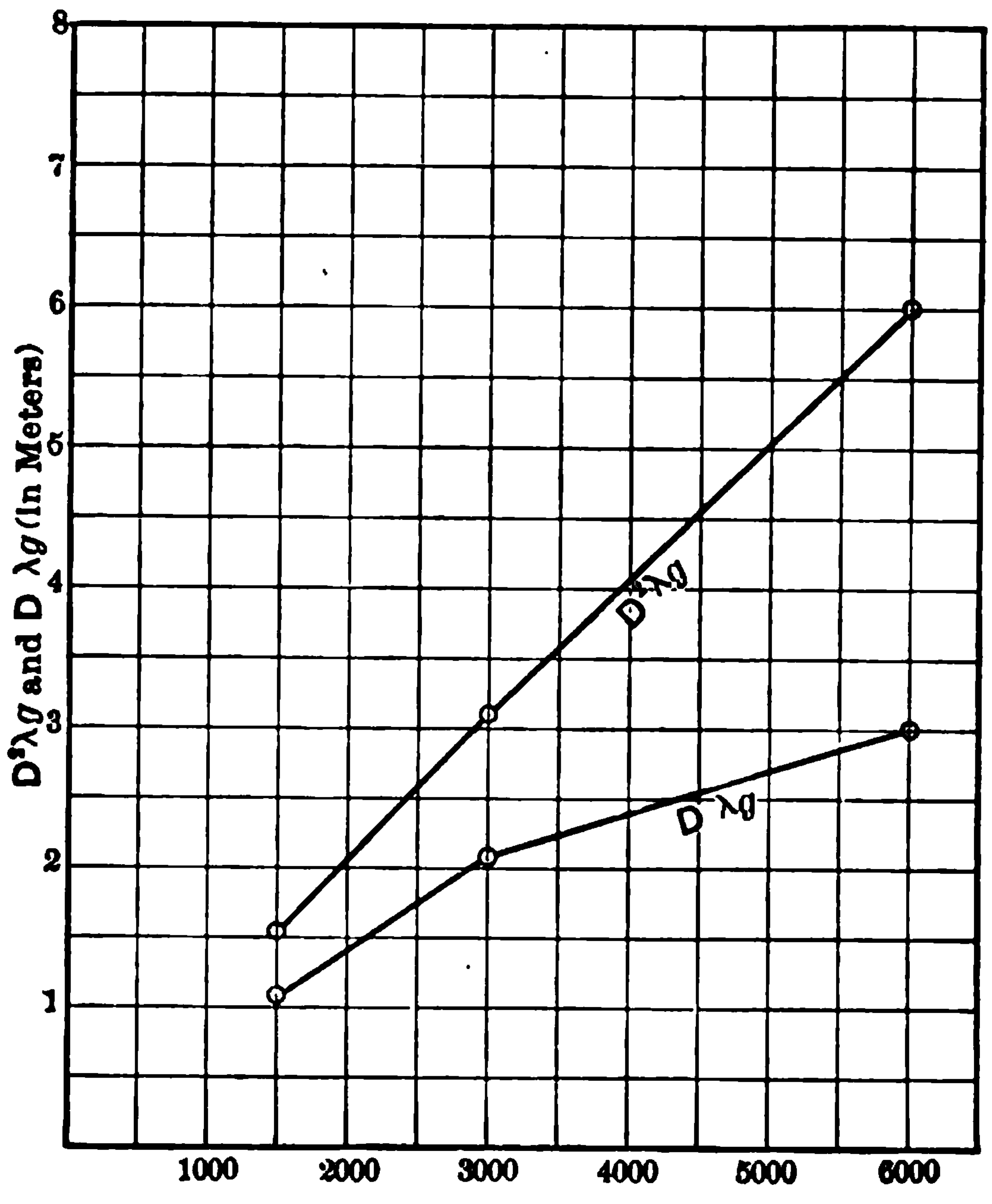


FIG. 128.— $D^2\lambda g$ and $D\lambda g$ for 11000-volt 3-phase 8-pole 50-cycle alternators.

as already shown in Chapter IX. Above 1000 R.P.M. and 6 poles, one is, however, getting into the undesirable range of speed for 3000 kva. The 6000 kva. design has probably its most suitable speed

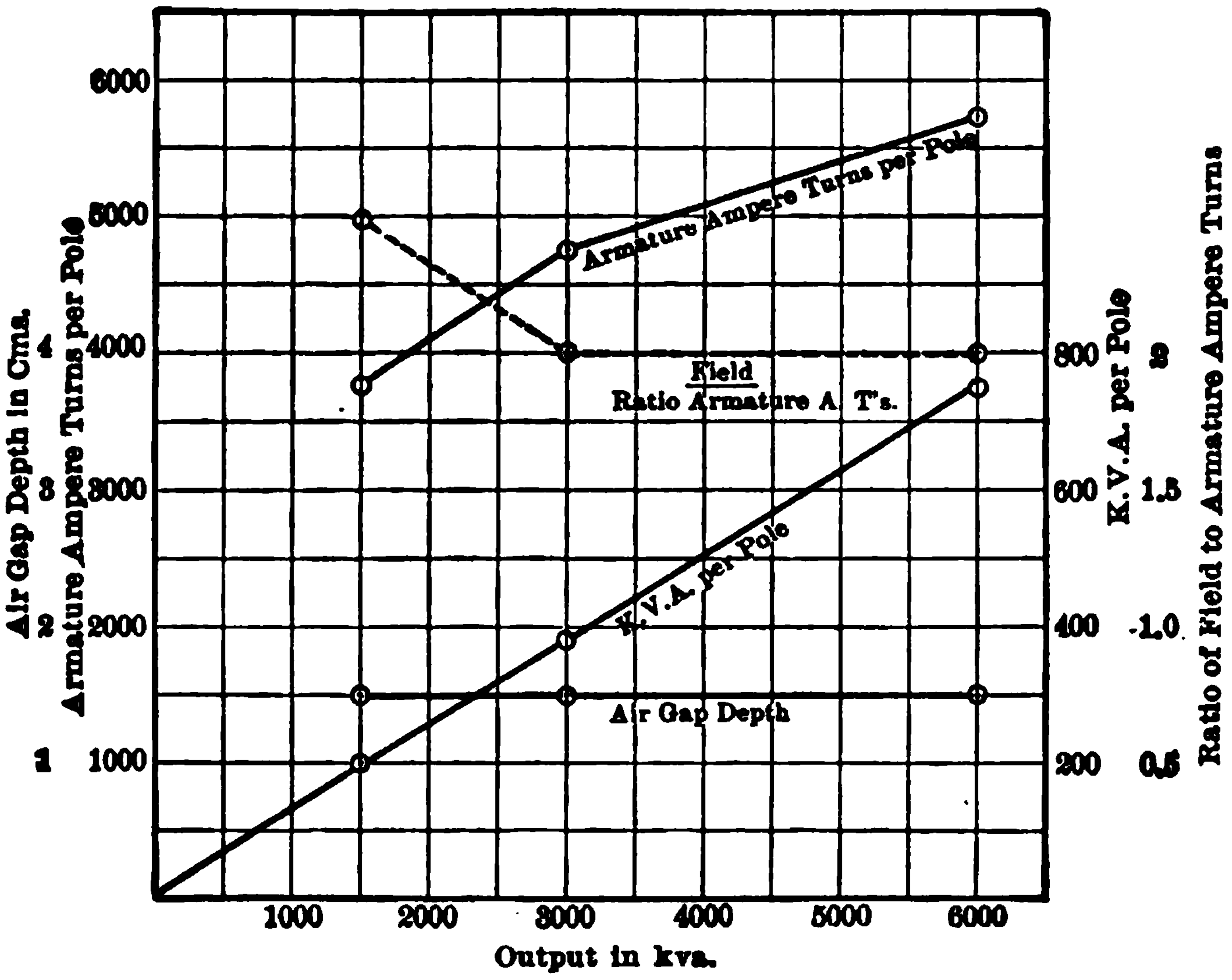


FIG. 129.— Armature ampere turns and kva. per pole and air gap length and ratio of field to armature ampere turns for 11000-volt 3-phase 8-pole 50-cycle alternators.

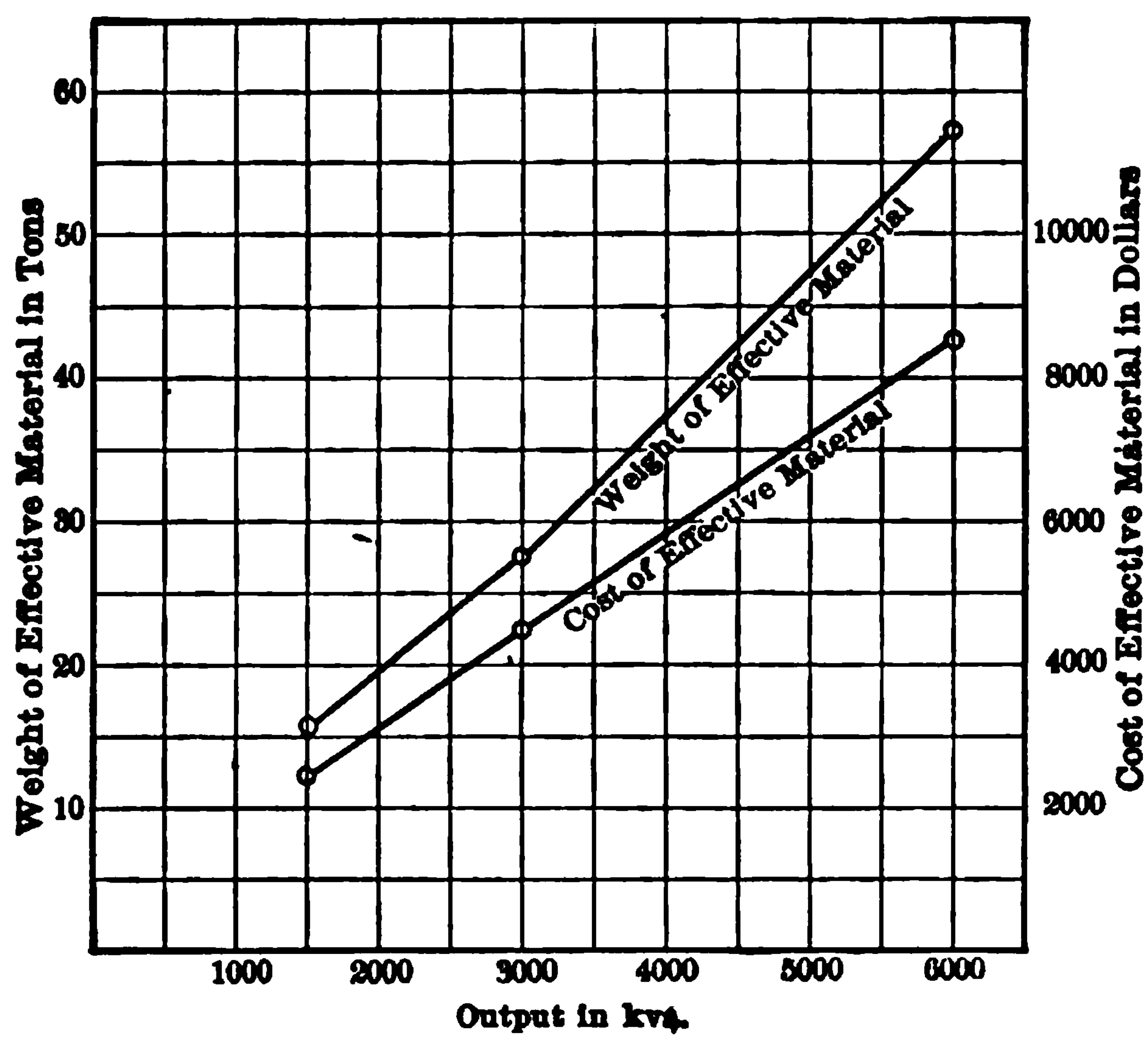


FIG. 130.— Weight and cost curves for 11000-volt 3-phase 8-pole 50-cycle alternators.

at 750 R.P.M. as a higher speed and fewer poles would bring the design into the range of undesirability. While the necessary comparisons may be drawn from the tabulated specifications and drawings, we have in Figs. 128 to 132, plotted several of the quantities of chief interest.

Fig. 128 shows the values of $D^2\lambda g$ and $D\lambda g$ plotted against the rated output. $D^2\lambda g$ is practically proportional to the rated output as the output coefficient, ξ, is practically the same throughout.

In Fig. 129 are plotted the armature strength, air gap depth, and ratio of field to armature ampere turns. The remarkable point is

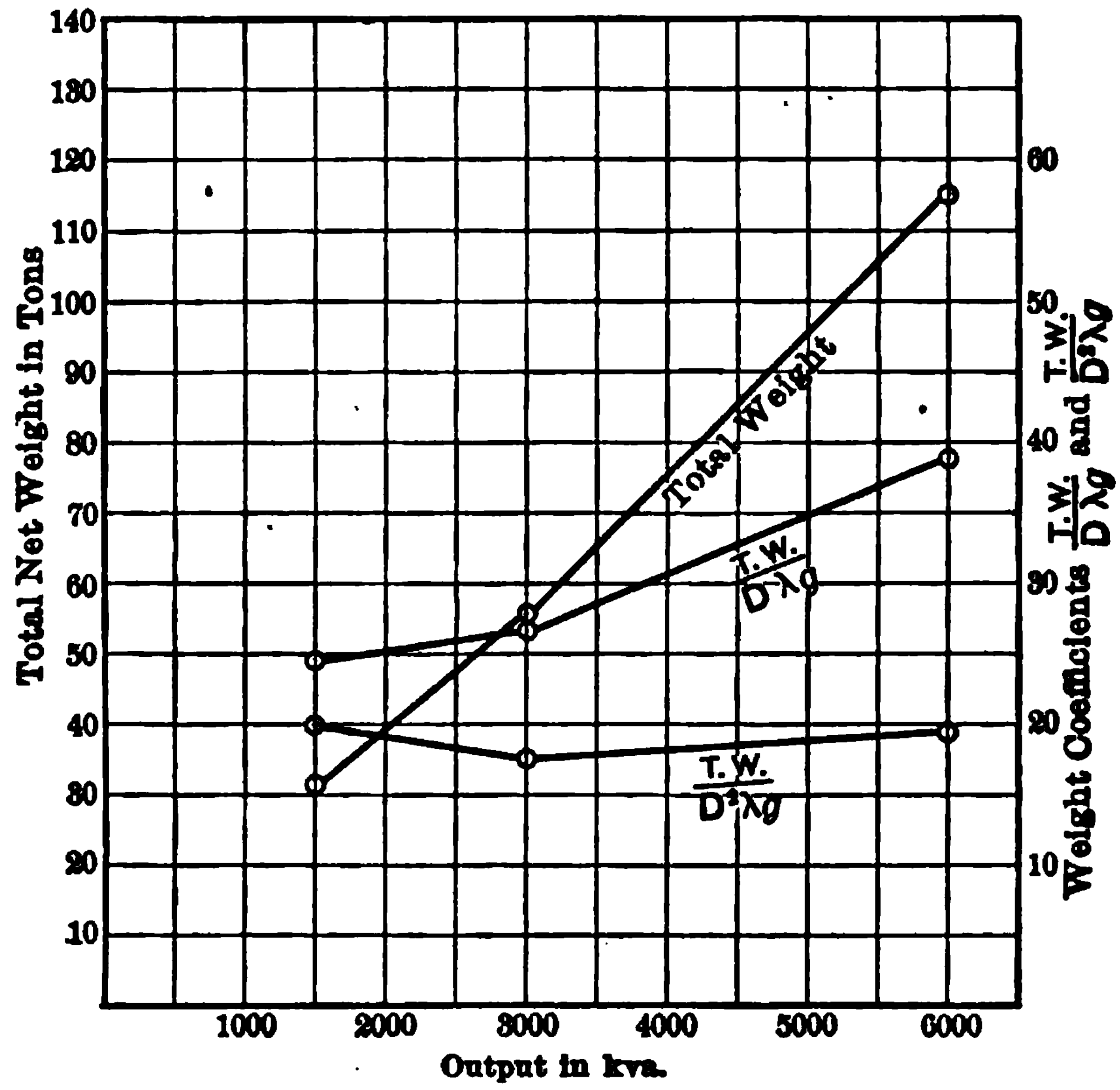


FIG. 131.— Total net weights and weight coefficient for 11000-volt 8-pole 50-cycle 3-phase alternators.

that the air gap depth is constant. This is in this case due to the 1500 kw. machine having a lower degree of saturation and air gap density, and a higher ratio of field to armature ampere turns.

Fig. 130 shows the weight and cost of effective material, which are, as would be expected, approximately proportional to the rated output.

Fig. 131 shows the total weight derived from an appropriate value of the weight factor of 2.0 in each case. The weight coefficients are also plotted in Fig. 131.

In Fig. 132 are shown the total weight per kva. output and the weight and cost of effective material per kva. output. The chief interest in these curves is the uniformity of the weight and cost per kva. output which are only slightly higher in the smallest rating. This conclusion will only be applicable for machines of the same speed and number of poles.

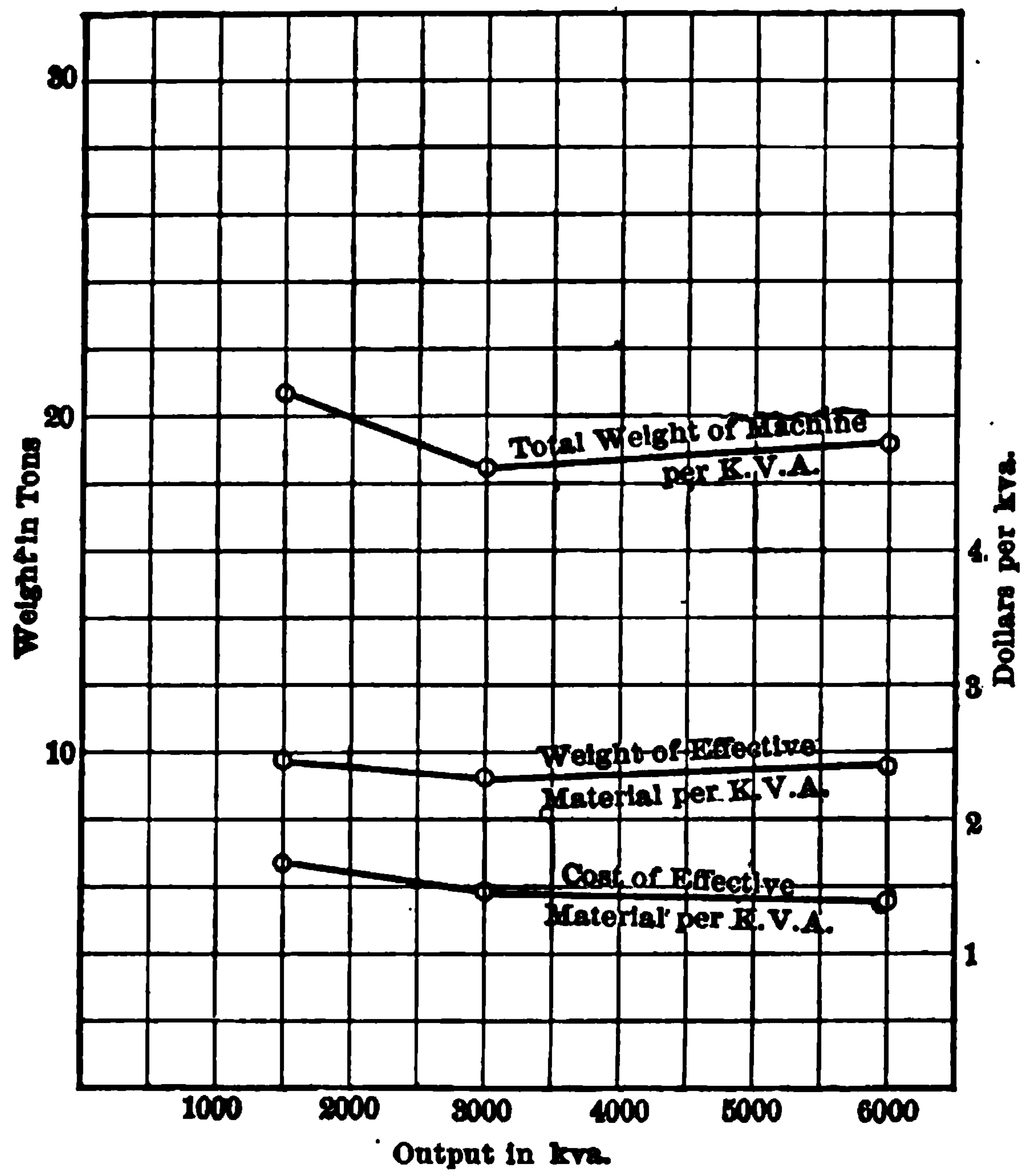


FIG. 132. — Total weight of machines and weight and cost of effective material per kva. for 11000-volt 8-pole 50-cycle 3-phase alternators.

We are now in a position to set forth certain general conclusions as to the weight and cost per kva. in terms of various rated speeds and outputs, and these conclusions will be chiefly based on the designs which we have described in these chapters.

General Conclusions Regarding the Relation of Rated Speed and Rated Output.

High speeds are not unfavourable to alternating current designs, and, when not carried to excess, the higher the speed the less will be the weight and cost of the machine.

There is a certain speed, or a certain range of speed, for a given rated output and frequency, at which the most economical and best design is obtained. The value of this speed is dependent on the number of poles which must be associated therewith to give the required frequency. In general, designs for rated outputs up to 2000 to 3000 kva. are most favourable and economical when the design has a speed corresponding to 4 poles. For rated outputs higher than 3000 kva., the most favourable speed is that corresponding to 6 or 8 poles, the lower speeds applying best to the larger rated outputs.

The most appropriate speeds for different ratings are as set forth in the following table, to which are also added the approximate speeds of steam turbines of corresponding rated output from the curves in Fig. 1.

TABLE 40.

THE MOST APPROPRIATE SPEEDS FOR ALTERNATING CURRENT GENERATORS OF VARIOUS RATINGS AND CORRESPONDING STEAM TURBINE SPEEDS.

Rated Output kva.	25 Cycles.		50 Cycles.		Corresponding Average Steam Turbine Speeds.	
	Number of Poles.	Speed R.P.M.	Number of Poles.	Speed R.P.M.	Parsons Turbine.	Curtis Turbine.
750	4	750	4	1500	2000	1500
1500	4	750	6	1000	1500	750
3000	6	500	6 8	1000 750	1000	650
6000	6 8	500 375	8 12	750 500	750	500

It is interesting to note from these figures that the steam turbine speeds are more favourable to alternator designs than to continuous current designs (see page 3, Chapter 1). Further, the speeds of the Curtis* turbine, which are considerably lower than those of the Parsons turbine, are nearer the most appropriate speeds for the alternator designs of corresponding rated output.

* Developments are occurring so rapidly in steam turbine manufacture that this statement as to the relative preferred speeds of Curtis & Parsons turbines may be upset at any time. Indeed we have observed indications that the manufacturers of the Curtis type are tending upward in speeds, and that the reverse tendency is taking place in the Parsons type as manufactured by some of the many licensees.

CHAPTER XI.

CONSTRUCTION OF HIGH SPEED ALTERNATORS.

High speed alternators are almost invariably of the rotating field type. The rotating armature type is only employed in certain cases where the rated output is small and the speed very high. In such cases it has certain advantages as noted in Chapter VIII. In machines for large outputs the brush collection of heavy currents at high voltages becomes objectionable, and in machines for these outputs at lower voltages, the required proportions of brushes and rings become utterly impracticable.

Stator Frames. — The frames for high speed alternator armatures do not present such serious mechanical problems as those for slow speed flywheel alternators. The latter have to constitute a rigid structure in themselves and support the armature core without deflection. Their design for the very large diameters involved in slow speed work is a problem in structural mechanics.

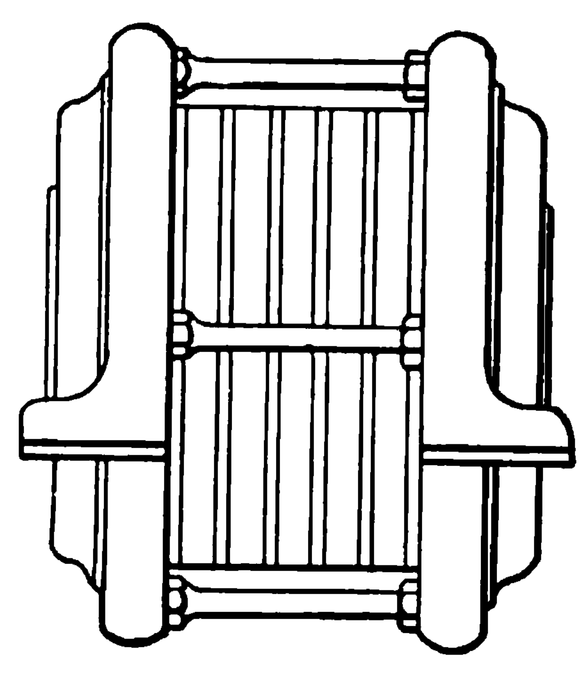


Fig. 133. — Open type stator frame.

In alternators for steam turbine speeds the general proportions are very different, as may be seen from any of the high speed designs dealt with in the previous chapters. The air gap diameter, D, rarely exceeds 2 metres, and the overall diameter of the frame in existing types does not exceed 4 metres. The overall length is on the other hand large, reaching and exceeding 2.5 metres in large sizes. In such frames the precautions necessary for rigidity are not nearly so great as in frames of larger diameter. The stiffness is contributed to by the rigidity of the armature core owing to its great radial depth, which usually amounts to 0.3 or 0.4 of the pole pitch.

The fundamental problems in connection with frames for high speed alternators relate almost exclusively to ventilation, and not to struc-

tural considerations. The arrangements employed for ventilation practically determine the general design of the frame.

There are two alternative schemes for ventilating high speed machines. The first consists in exposing as freely as possible to the air, all parts of the machine in which losses occur and from which heat has to be dissipated. The second consists in completely enclosing the machine in a smooth case leaving an entry for air either at

Fig. 134.—Alternator with open frame—(Allgemeine Electricitäts Gesellschaft).

the shaft or at the base, and an outlet at the top of the machine or at some other suitable point.

The latter plan has now come to be most widely employed, and it may be termed "forced" or "induced" ventilation. The draught is induced as in a chimney, in fact in certain cases an actual chimney has been placed at the top aperture of the machine to assist the draught. The natural circulation is usually strongly reinforced by

the effect of the rotor which is often supplied with fan blades. This scheme may be carried so far as cooling by air blast from a separate blower set, either for each generating set or for a number of sets, as in the case of batteries of transformers.

The extreme case of the first scheme is illustrated by the con-

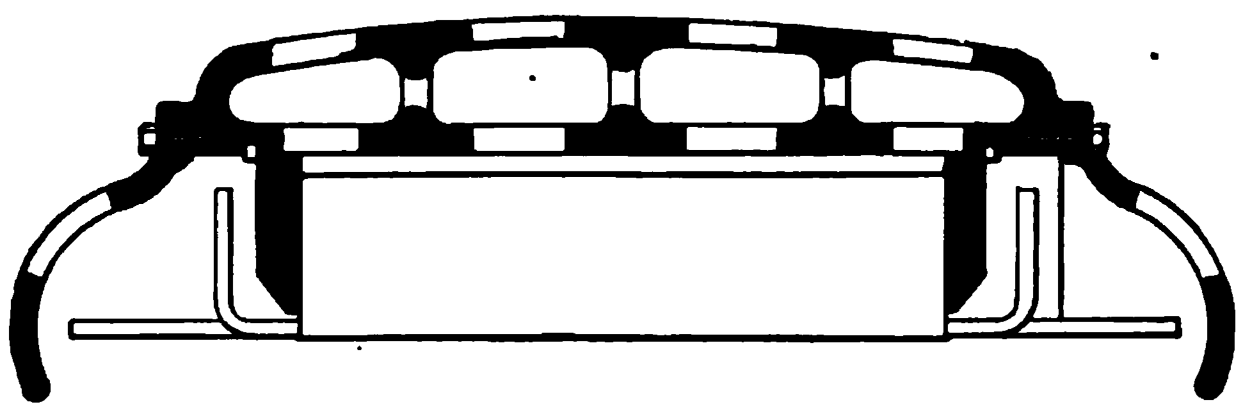


FIG. 135. — Section of Westinghouse turbo-alternator stator.

struction shown in Figs. 133 and 134. Figs. 135, 136, and 137 show sections through frames constructed for open ventilation. Fig. 135

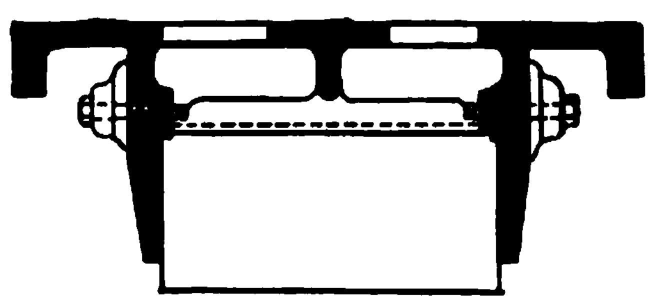


FIG. 136. — Section of B. T. H. turbo-alternator stator.

is the Westinghouse Co.'s construction, and Fig. 136 the General Electric (of America) and the British Thomson-Houston Co.'s. In

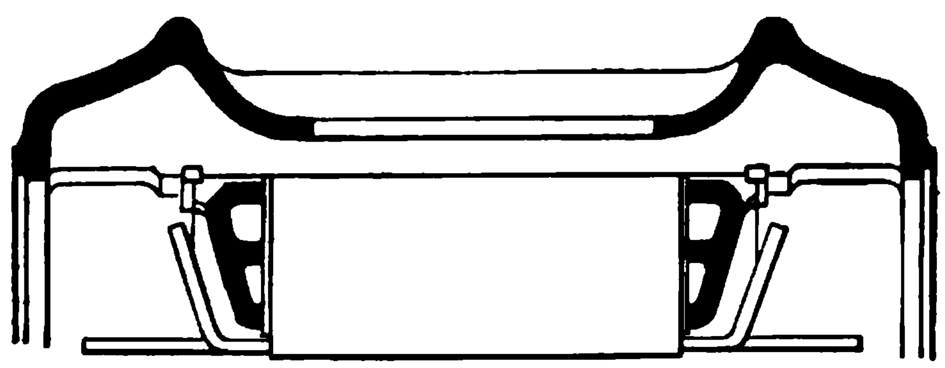


FIG. 137. — Section of ribbed stator frame.

the latter type the frame is not split, the Curtis turbine with which the design is used, having a vertical shaft. Fig. 137 is of the same type which has already been shown in Fig. 77 of Chapter VII. This frame is stiffened by longitudinal ribs on the outside.

A typical stator case designed for forced ventilation is shown in Fig. 138 which is one of the Lahmeyer Co.'s construction. The lower part of the end shield is so shaped as to form an inlet air channel, and an air exit at the top of the frame is provided.

Fig. 139 shows a group of frames of various types by Messrs. Lahmeyer, all of which are constructed for induced ventilation. The machine shown in the foreground differs from that in Fig. 138 in

FIG. 138. — Enclosed frame for forced ventilation — Lahmeyer Co.

that the air enters through apertures in the *sides* of the end shields, while in Fig. 138 it enters from the base of the machine.

In these types of machine the draught is generally induced by means of vanes or fans placed at each end of the rotor. Such vanes will be seen in the rotor constructions of Figs. 178, 188, 189, 190, 222 and 226 of this chapter.

Fig. 140 shows a section through a construction of Messrs. Brown Boveri showing the direction of the air circulation. This is on the

FIG. 139. — Group of Lahmeyer turbo-alternator stators.

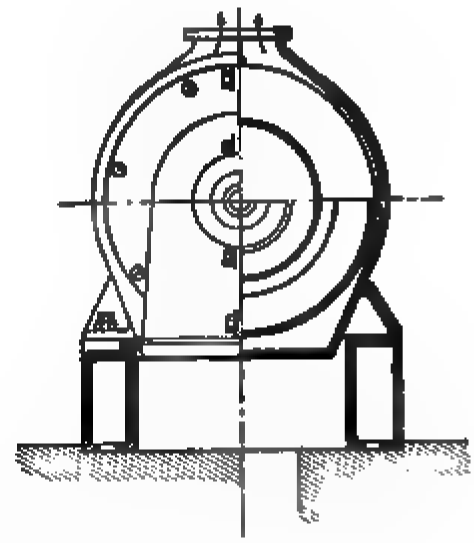


FIG. 140.— C. E. L. Brown's ventilating scheme for turbo-generator.

same lines as the construction in Fig. 138, and more details will be found in the assembly drawing of Fig. 217.

A ventilating scheme by the Oerlikon Co. is illustrated in the drawings of Figs. 141, 142, and 143. The principal feature of this

FIG. 141.— Oerlikon Co's ventilating scheme for turbo-generators.

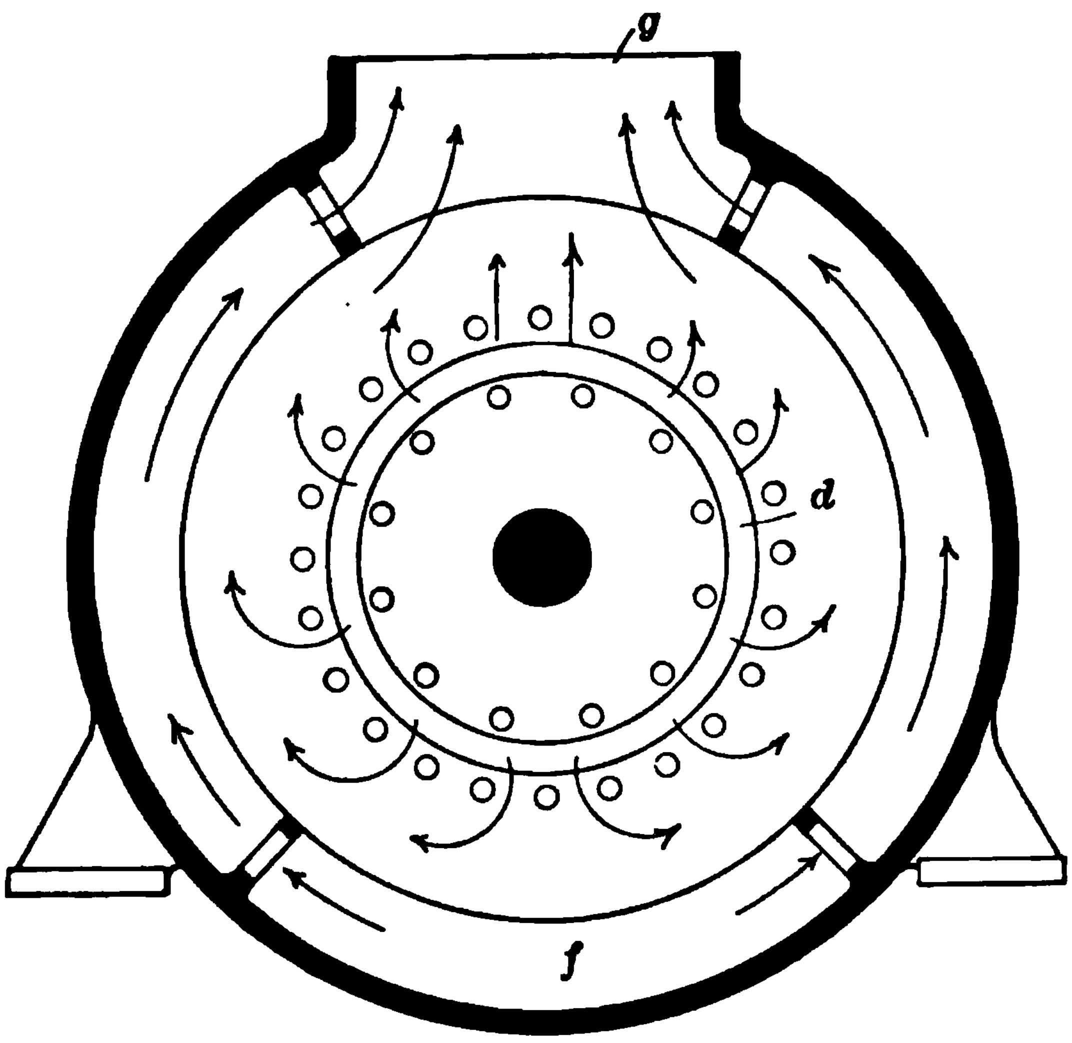


FIG. 142. — Oerlikon Co's ventilating scheme for a turbo-generator.

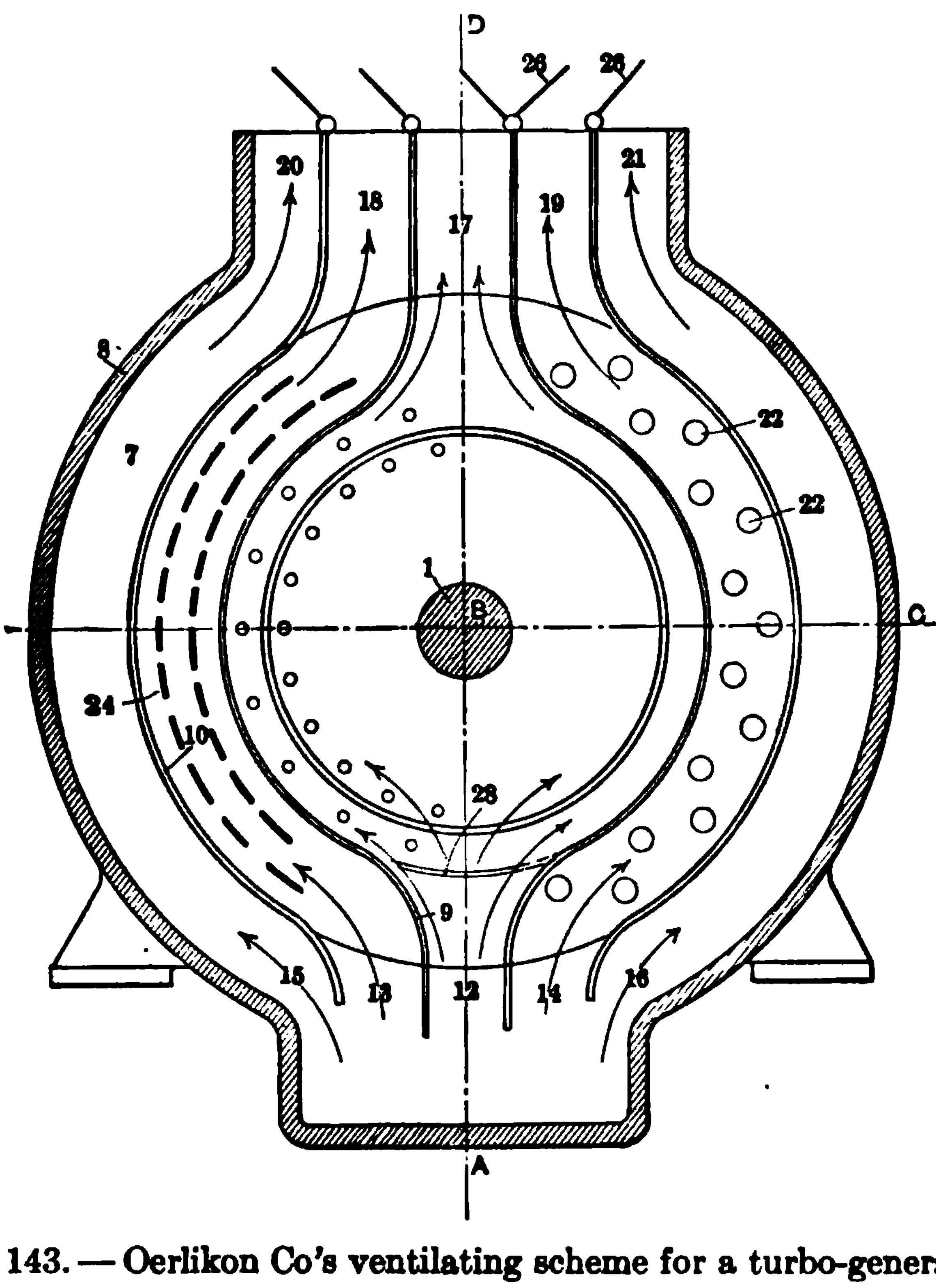


FIG. 143. — Oerlikon Co's ventilating scheme for a turbo-generator.

system is the subdivision of the air channels through which the air is directed. The blades on the rotor are so placed as to represent an ordinary centrifugal fan, and the air thus drawn into the machine is passed partly through the rotor, partly through the air gap, and thus through the ducts in the stator, and partly over the end connections, and at the back of the stator laminations. Finally the air is united in a common channel and passes through a large hole in the top of the machine. Figs. 144, 145, and 146 illustrate the armature frame

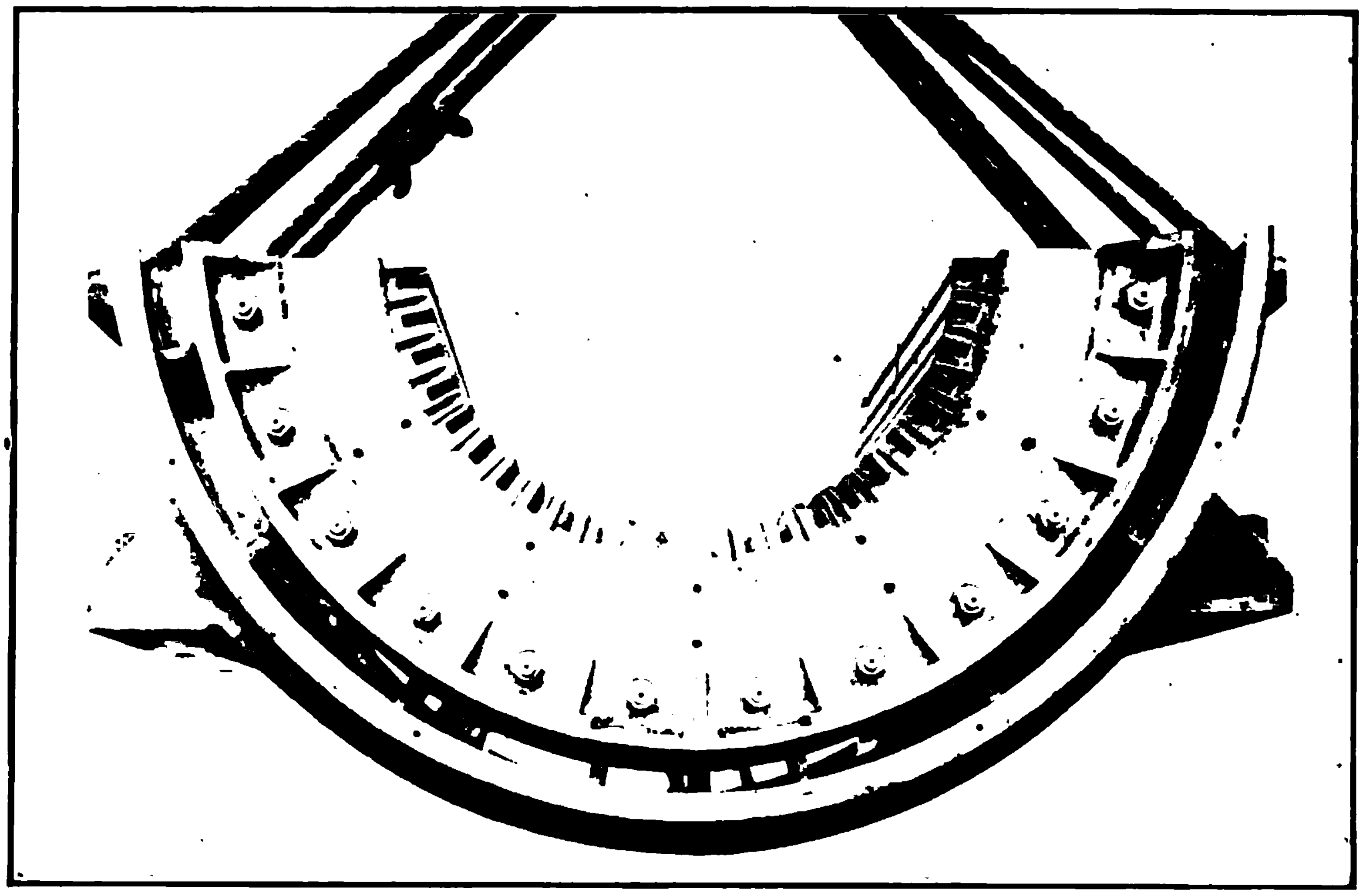


Fig. 144.— Lower half of frame of 1100 kw. 1500 r.p.m. alternator — Oerlikon Co.

of an 1100 kw. 1500 R.P.M. alternator arranged in accordance with this ventilating scheme.

These photographs also illustrate a typical construction for stator frames. Further details of the Oerlikon Co.'s machines will be found on pages 251 to 255 of this chapter.

Fig. 147 shows an example of an enclosed frame by Messrs. Siemens Bros. This machine has forced ventilation, air being drawn in through a trunk at the end remote from the turbine by means of fans on the rotor ends. This trunk is connected with the outside air by pipes. The air, after circulating through the machine, passes out

through an opening in the frame arranged either at the top or at the bottom as desired.

The problem of ventilating and cooling high speed dynamos is very like the corresponding problem for transformers, and many of the means which have been adopted in connection with the latter are being employed for high speed dynamos.

FIG. 145. — Lower half of frame of 1100 kw. 1500 r.p.m. alternator — Oerlikon Co.

Fig. 148 gives an instance of a water cooled alternator by the Allegemeine Electricitäts Gesellschaft. The stator frame consists of a cylindrical chamber surrounding the armature core. Water is circulated through this chamber, and the heat dissipation is facilitated by circumferential ribs cast on to the inner shell.

In connection with the subject of circulation, reference should be

made to the section of this chapter dealing with rotor construction, where many special arrangements for ventilating rotors and providing for air circulation through the rotor and stator will be seen.

Armature Windings for Turbo-Alternators.

The windings for turbo-alternator armatures do not differ in principle from those employed for slow speed alternator armatures, but the turbo-windings are characterised chiefly by the largeness of the dimensions of the coils by reason of the large pole pitches and arma-

FIG. 146. — Assembling upper half of armature of 1100 kw. 1500 r.p.m. alternator.

ture core lengths, and also by the greater distribution of the coils incident to the large number of slots per pole per phase.

The large values of the pole pitch generally render bar windings and barrel windings impracticable, since, owing to the great size of the coils, they would project to a considerable distance from the ends of the core if windings of this type were employed.

If the speed is only moderately high and the machine has more than eight poles, bar windings, or barrel windings, may, however, be used. In the classification of alternating current windings * adopted by the

* See "Armature Construction," Hobart and Ellis (Whittaker & Co., London, 1907), p. 197.

FIG. 147.—Turbo-alternator with forced ventilation. (Built by Messrs. Siemens Bros. Ltd.)

authors, a "coil" is designated as "the group of turns subtending one pole belonging to one phase," irrespective of the number of turns in the coil.

Fig. 148.— Section of an Allgemeine Electricitäts Gesellschaft turbo-alternator, water-cooled.

This "coil" may have its conductors concentrated in a single slot on each side, or distributed on each side in two, three, four, or more slots. In turbo-alternator armatures the coils are very distributed as noted above. If the conductors on one side of the coil lie in only one

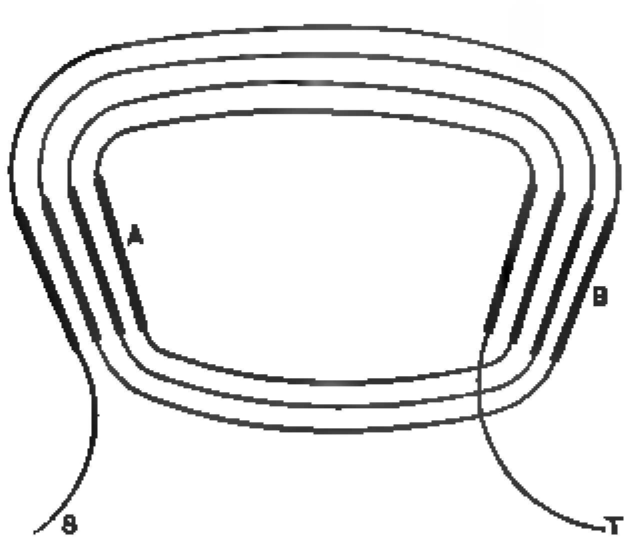


Fig. 149.— Spiral coil.

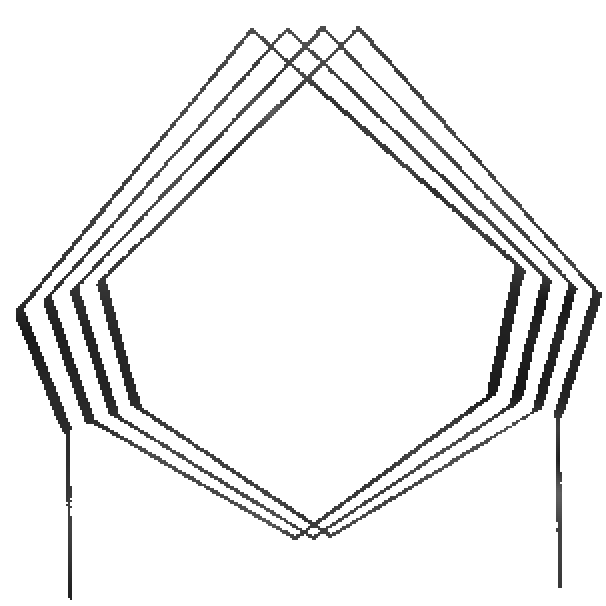


Fig. 150.— Lap coil.

slot, we designate the coil as a "single coil," if they are spread over two slots we call it a "double coil," if over three slots a "triple coil," and so on.

The coil itself may be arranged in two ways, which are designated "spiral coil" and "lap coil." A spiral coil is illustrated in Fig. 149. This coil has four turns, and the equivalent lap coil is shown in Fig. 150.

Fig. 149 would be designated an element of a quadruple coil spiral winding, and Fig. 150 an element of a quadruple coil lap winding.

In the lap coil the "pitch" of the conductors is the same for all pairs of conductors connected together. A winding having coils shaped in either of these two ways will be called a "coil winding." There are,

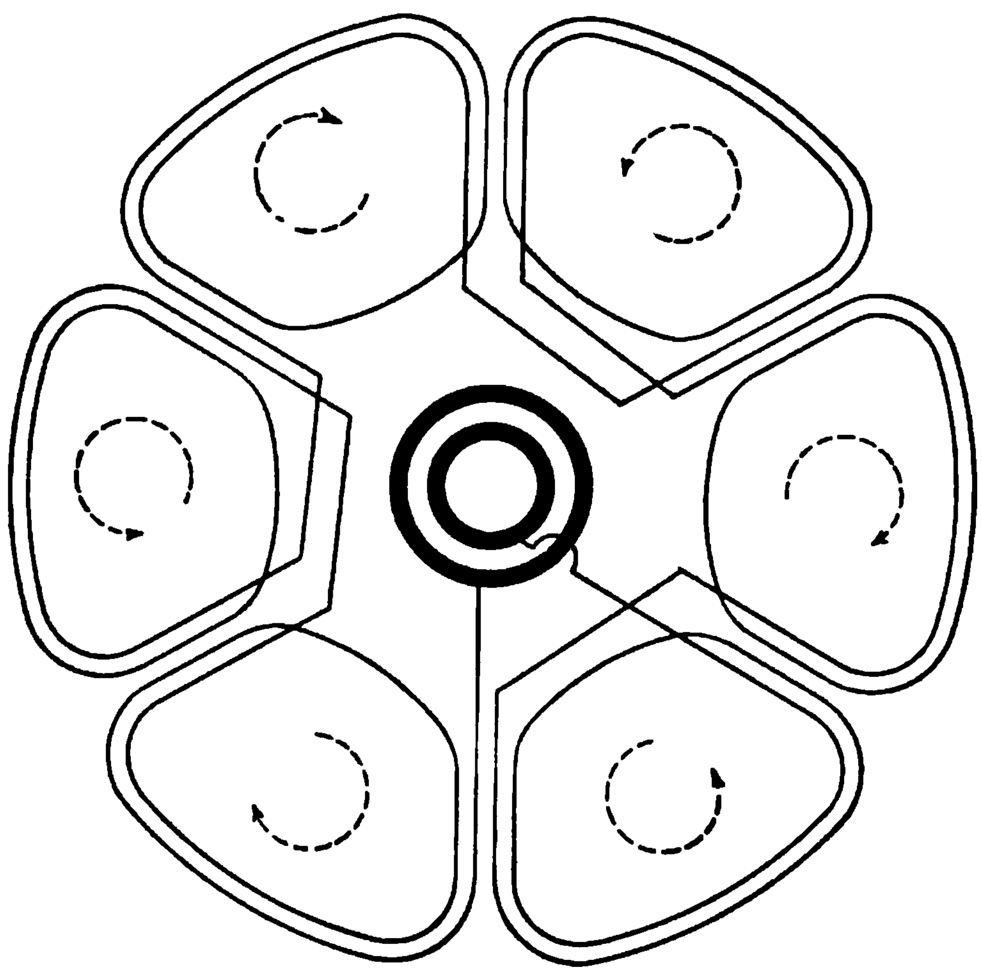


FIG. 151.— Single-phase whole coiled winding.

however, two broad divisions of coil windings which are respectively termed "whole coiled" and "half coiled."

In the whole coiled winding there is one coil per pole per phase, *i.e.*, every pole is subtended by a coil; but in the half coiled winding there is only one coil per phase per *pair* of poles; that is to say, only half of the poles are subtended by coils. The distinction between these two types will be apparent from Figs. 151 and 152. Each of these figures represents a 6-pole single-phase winding, which is taken for simplicity. Fig. 151 has every pole subtended by a coil of 2 turns. This winding would be designated a 6-pole whole coiled double coil spiral winding, and Fig. 152 would be called a 6-pole

half coiled quadruple coil spiral winding. The number of slots per pole is in each case 4, but in Fig. 151 the coil is only a double coil, whereas in Fig. 152 it is a quadruple coil.

Corresponding polyphase windings can readily be derived from the single-phase windings in Figs. 151 and 152 by superimposing one or two extra phases, according to whether a 2-phase or a 3-phase winding is required. Single-phase windings are more often whole coiled, especially in turbo machines where there are few poles and consequently few coils.

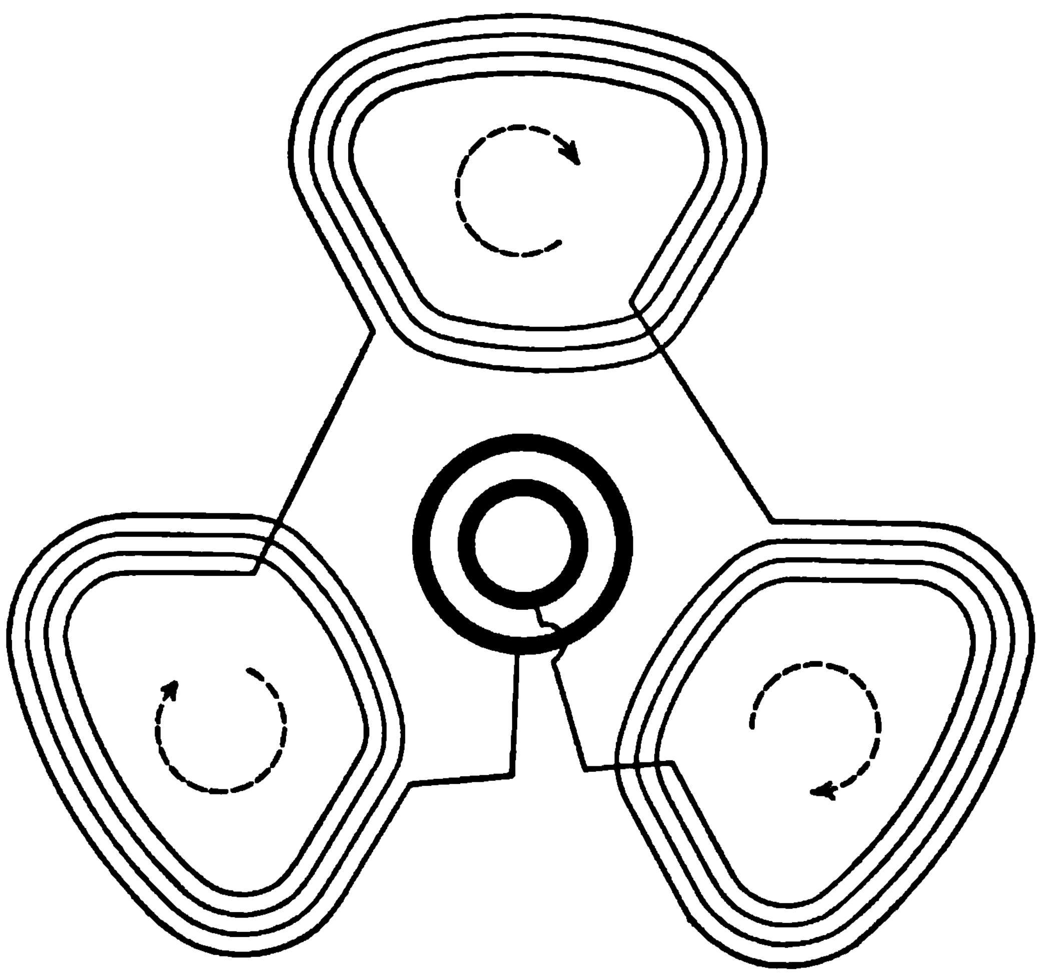
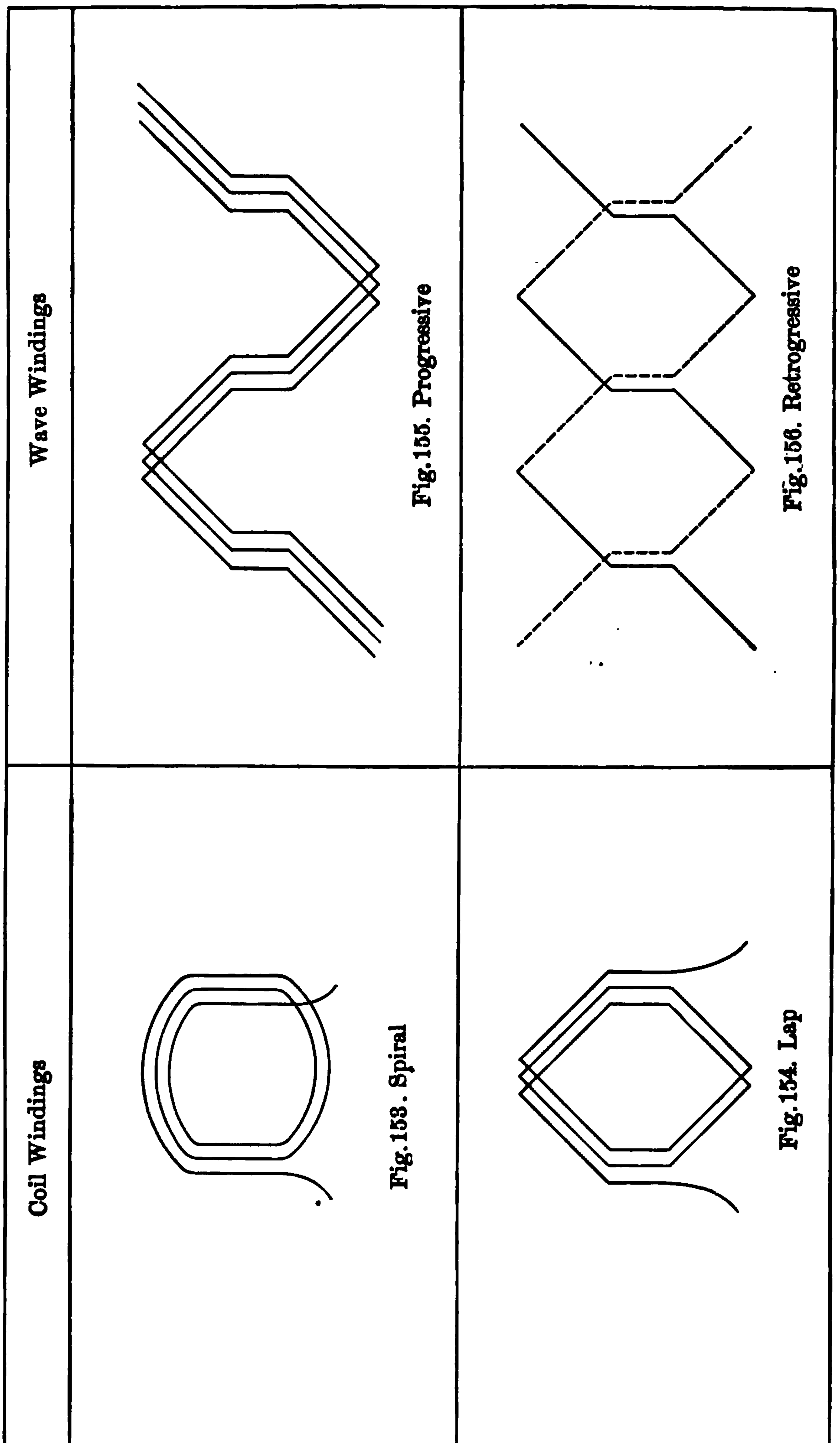


FIG. 152.— Single-phase half coiled winding.

If one inspects the end connections at the inner side of the diagrams in Figs. 151 and 152, one sees that the connections in Fig. 152 are considerably simpler than those in Fig. 151.

The relative directions of current circulation in the individual coils are represented by dotted circular arrows at the centre of each coil, and it will be seen that in the half coiled winding (Fig. 152) the circulation is in the same direction in each coil, whereas in Fig. 151, the whole coil winding, the direction is reversed in every alternate coil. Thus in Fig. 152 it is only necessary to connect the end of one coil on to the beginning of the next adjacent coil, which is simpler than the connection in Fig. 151.

So far as single-phase windings are concerned, a disadvantage of the half coiled winding is that the end parts of the coils are more



FIGS. 153-156. — Elements of alternating current armature windings.

concentrated than in the whole coiled winding. This leads to somewhat greater inductance of the end connections, and to greater heating of the end connections. Hence for single-phase windings whole coiled windings are preferable. For polyphase windings,

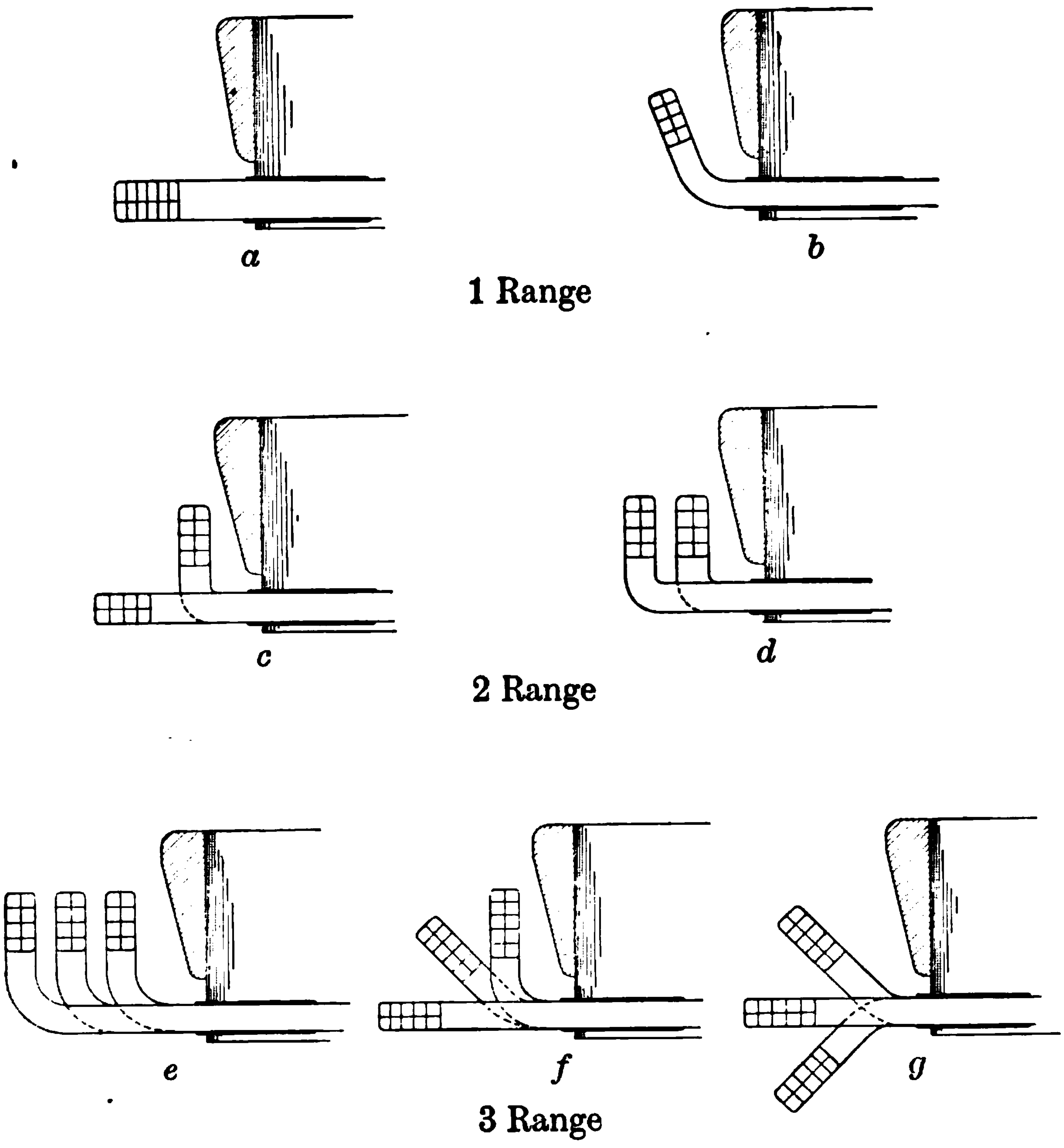


FIG. 157.— Sections of armature end windings.

however, certain features of the half coiled windings have led to its general use. Furthermore the inductance of turbo-alternators is inherently low.

The alternative method of winding to coil winding is wave winding. Elements of the two common forms of wave windings are shown in Figs. 155 and 156. These are designated respectively progressive and retrogressive wave windings, and the distinction between the two forms will be clear from the figures. The retro-

gressive winding requires the number of conductors per slot to be a multiple of two, as half of the conductors (in the top of the slots) are taken in in the first progression round the winding, and the other

FIG. 158.— Wound bipolar armature of Oerlikon 400-kw. 5000-volt 42-cycle 2520 r.p.m. turbo-alternator with a 2-pole 2-phase whole coiled, sextuple coil spiral winding.

half (in the bottom of the slots) in the progression in the opposite direction.

We have grouped together in Figs. 153–156 the two common types of coil windings and the two common types of wave windings. There are a variety of other types of windings * which should be regarded as special types, as their application is not general.

* See "Armature Construction," Hobart and Ellis (Whittaker & Co., London, 1907), Chap IX, for a much more complete treatment of the subject of armature windings.

In carrying out polyphase windings the matter of the number of ranges in which the coils can be laid up enters. Fig. 157 illustrates sections through the end parts of several windings which may be designated as respectively 1 range, 2 range, and 3 range windings.

Single-phase windings are always 1 range and 2-phase windings are always 2 range; 3-phase windings may be either 2 range or 3 range according as the winding is half coiled or whole coiled. The

Fig. 159. — Armature of Westinghouse 5500-kw. 4-pole 1000-r.p.m. 33-cycle 11000-volt turbo-alternator with a 4-pole 3-phase half coiled octuple coil spiral winding.

coils may be laid up in any of the shapes illustrated in Fig. 157. With turbo-alternator armatures the coils are either laid up in 2 or 3 perpendicular ranges, or else in one range projecting straight out from the slots, the other range being laid back at right angles to the air gap surface.

Fig. 158 illustrates a 2-phase stationary armature for a 400-kw. 5000-volt 42-cycle 2520 R.P.M. Oerlikon turbo-alternator. This

winding is a 2-pole whole coiled sextuple-coil spiral winding. It has 48 slots and 12 slots per pole per phase. There are 2 coils per phase

Fig. 160.— Winding armature of 5500 kw. Westinghouse alternator.

(1 coil per pole per phase), and 4 coils in all. These are laid up in 2 ranges with 2 coils in each range. The mode of laying up the

end coils corresponds to Fig. 157*d*. Fig. 159 illustrates a finished armature of one of the 5500-kw. 1000 R.P.M. 33-cycle Westinghouse alternators of the Chelsea Station of the London Underground Electric Railway Co. This winding is a 4-pole 3-phase half coiled octuple coil spiral winding. There are 8 slots per pole per phase and 96 slots in all. As this winding is half coiled, each coil covers

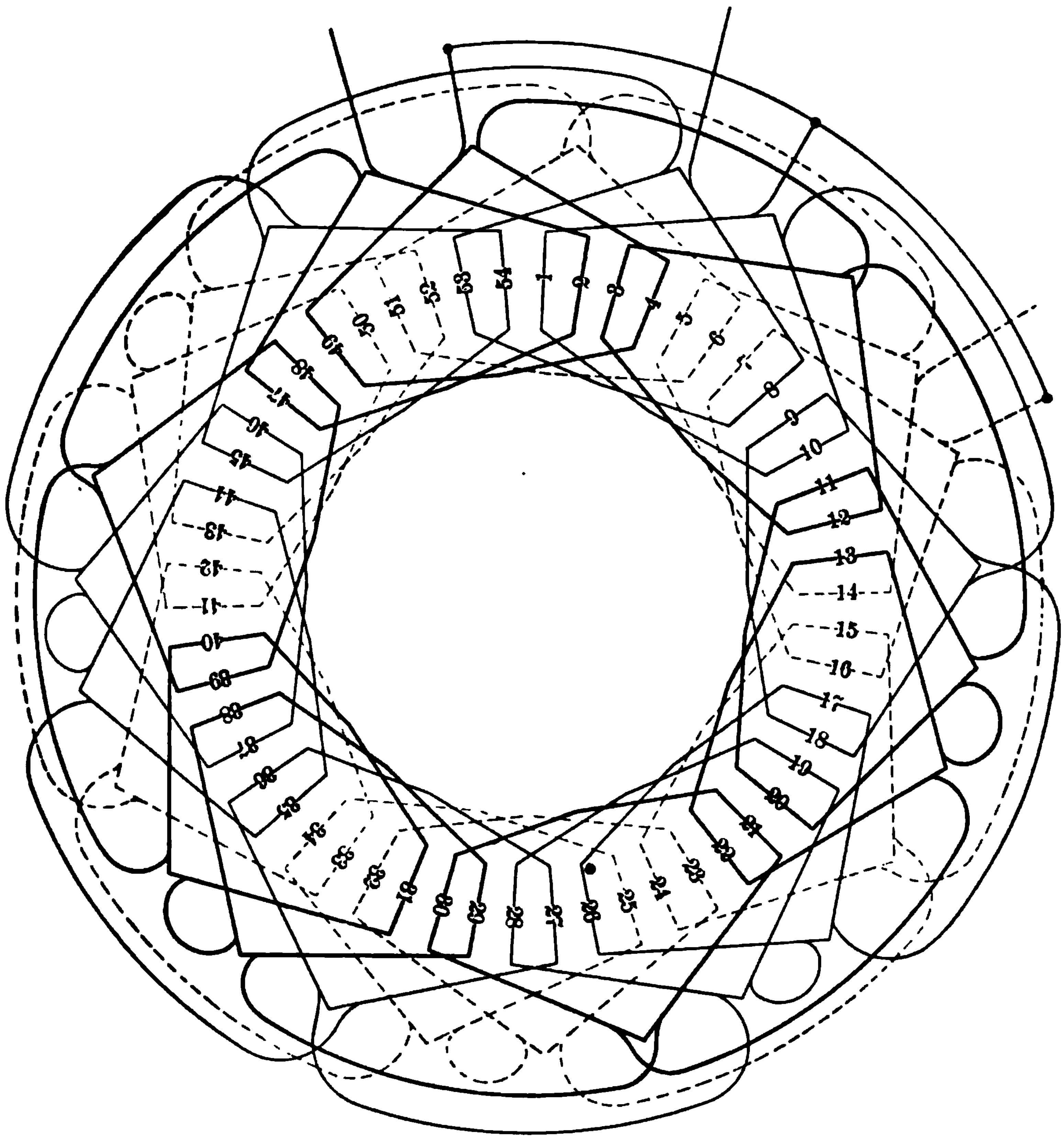


Fig. 161.— Armature winding of 1500-kw. 1000 r.p.m. 6-pole 50-cycle British Thomson Houston Curtis turbo-alternator.

8 slots on either side. There are 2 coils per phase and 6 coils in all, laid up in 2 ranges in the manner of Fig. 157*c* with 3 coils in each range. Fig. 161 illustrates one of the armatures in process of winding. The armature is hand wound, the back range being put on first, and after that the second range which projects straight out from the slots.

It is possible to arrange windings intermediate between whole coiled and half coiled in certain cases, and sometimes such intermediate

Fig. 162 — Armature winding and slot of 1500 kw. British Thomson-Houston alternator.

arrangements permit of all the coils being shaped on the same form which is, of course, an advantage when a former winding is employed.

Fig. 161 shows the winding diagram for a winding carried out in this way. This is the diagram for the armature of the 6-pole 1500-kw. British Thomson-Houston alternator which is fully described on pages 284 to 295 of this chapter. In this winding three of the poles are subtended by two coils per phase, and the other three by one coil per phase, as will be seen from the phase which is lined in heavily in the diagram. The winding has 54 slots and 27 coils, thus

Fig. 163.

Fig. 164.

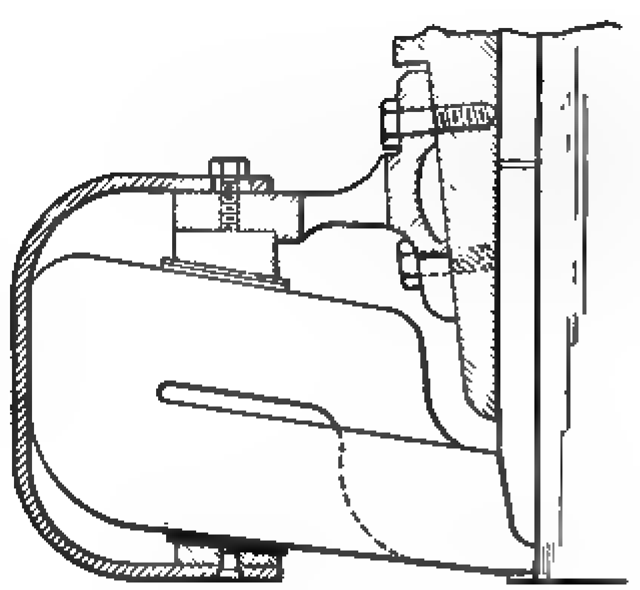


Fig. 165.

Figs. 163–165. — Methods of retaining end windings of turbo-alternator armatures.

9 coils per phase. The coils are formed to the shape shown in Fig. 162, and in this way they are made to lie up one above the other, as will be seen in Fig. 162. These coils are form wound, and are all of exactly the same shape. This type of winding is sometimes referred to as a "basket" winding, which is suggested by the appearance of the crossing of the ends of the armature coils. Further details of the winding and of the slots and conductor will be found in the description on pages 284 to 295 of this chapter.

In armatures with few poles and such large coils, the question of effectively retaining the ends of the coils becomes of importance. The large spans of the end windings are subjected to large magnetic pulls, especially on the occurrence of short circuits. The armature

FIG. 166.— Method of retaining armature windings of 3000 kva 4-Pole alternator — (G. E. Co. of America).

current, when a machine is suddenly short circuited, may momentarily be several times the value of the short circuit current which is obtained if the field strength is gradually brought up to the same value. As a machine is always liable to sudden short circuits, the

end windings are liable to be subjected to very heavy magnetic pulls.* It is therefore necessary to strap the coils back on to the armature end flanges in some good mechanical way. Fig. 163 illustrates in section a method of support of the end connections, employed by the Oerlikon Co., of a single-phase winding.

An Oerlikon method for polyphase generators is illustrated in the section drawing of Fig. 164, which shows a three range winding with the coils laid up in three parallel planes. The same method will be seen in Fig. 158, where the coils are secured by six wooden clamping pieces bolted on to the end flanges.

FIG. 167. FIG. 168.

FIGS. 167–168. — Methods of retaining armature end windings.

Fig. 165 illustrates the method employed with the special winding of the B.T.H. alternator given in Fig. 162. In this method a large ring is placed at the internal periphery of the end windings and is strapped back at several points by wrought iron straps on to the end flanges above the coils. Another instance of this construction is clearly shown in Fig. 166 which relates to a 3000 kva. 750 R.P.M. 4-Pole 13100 volt three-phase generator built for the Chicago & Milwaukee Electric Railroad Co. by the General Electric Co. of America. Figs. 167 and 168 illustrate two other alternative types

* See article by F. Punga on "The Sudden Short Circuiting of Alternators," *Electrician*, Aug. 31, 1906, vol. 57, p. 765.

of clamping brackets suggested for dealing with certain cases of windings where the coils have a very large span, as is especially the case with two-pole machines. Fig. 169 shows an armature of the Lahmeyer Company in which the coil retaining arrangements are similar to Figs. 167 and 168. Wherever practicable, far more substan-

FIG. 169. — Turbo-alternator armature of Lahmeyer Co. showing arrangements for securing end windings.

tial securing of the end connections should be adopted than is shown in any of these examples. This is a point of very great importance.

Two-pole alternators (and very often 4-pole alternators) generally have whole coiled windings whether they are single-phase or polyphase, as a half coiled winding with only 2 poles would give a very unsymmetrical winding which would be ugly mechanically. Thus, if a 2-pole 3-phase winding were arranged as a half coiled winding,

there would only be 3 coils altogether, i.e., one coil per phase per pair of poles, and such a winding would, mechanically, be very unsatisfactory.

Fig. 170 illustrates a 2-pole 3-phase alternator in process of wind-

FIG. 170.— Winding a 2-pole 3-phase armature— Bruce Peebles & Co.

ing. This winding is whole coiled, and has 2 coils per phase — 6 coils in all, laid up in three parallel ranges.

Figs. 171, 172, and 173 are examples of Messrs. Brown Boveri & Co.'s armatures showing windings of different types. Fig. 171 relates to a 5000-kw., 10,500-volt, 1000 R.P.M. 50-cycle 3-phase alternator. Fig. 172 relates to a 3000-kw. 3000-volt 1360 R.P.M.

45-cycle single-phase generator. Fig. 173 relates to a 1000-kw. 370-volt 1500 R.P.M. 50-cycle turbo-generator.

FIG. 171.— Armature of 5000-kw. 3-phase 10500-volt 1000-r.p.m. 50-cycle alternator— Brown Boveri and Co.

An interesting point in all these examples relates to the provision made for retaining the end windings. Designers cannot afford to neglect any practicable means for adding strength to the arrange-

ments employed for this purpose. The frame constructions shown in Figs. 171, 172 and 173 are also of interest.

FIG. 172. — Armature of 3000-kw. single phase 3000-volt 45-cycle 1360-r.p.m. alternator — Brown Boveri and Co.

Rotating Field Structures. — A great deal of attention has been given to the constructional problems arising in the design of rotating fields for high speed alternators. Several very satisfactory

designs have been evolved by the various manufacturing firms. There are many different possibilities of construction, depending

FIG. 173.— Armature of 1000-kw. 370-volt 1500-r.p.m. 50-cycle alternator—Brown Boveri and Co.

upon the individual requirements of each case, and we shall first deal with representative types at present employed, and then refer more in detail to the actual constructions employed by the different firms.

The material to be used depends to some extent upon the style of construction, and, speaking generally, the properties of the material as regards homogeneity, durability, elasticity, and tensile strength are all important factors. Castings should be avoided except in special

cases, or where the peripheral speed is low. Cast steel may sometimes be employed, but there is danger from blow holes. The preferable materials are varieties of wrought iron or forged steel of best quality.

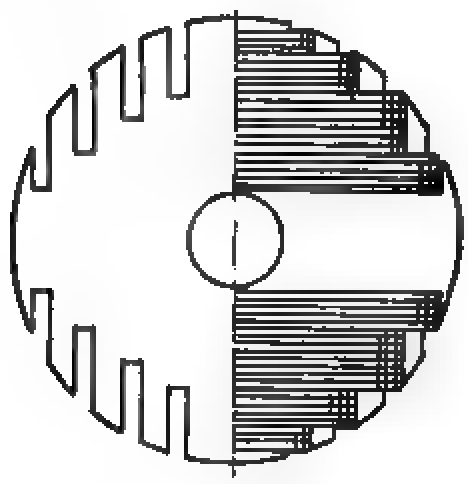


FIG. 174.

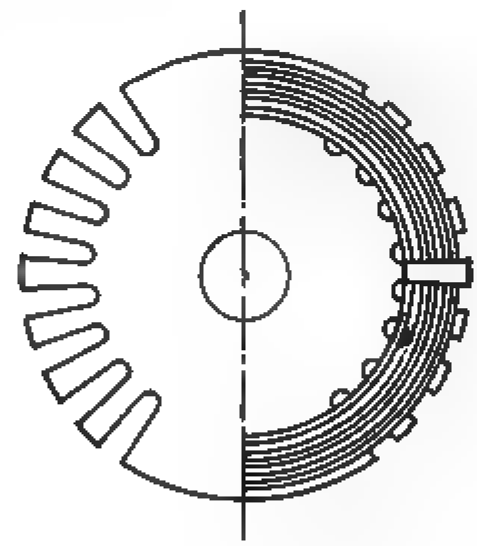


FIG. 175.

FIG. 176.

FIGS. 174–176.— C. E. L. Brown's cylindrical slotted rotating field constructions (from D. R. P. 138253 of 1901).

The large rotors of the 5500-kw. generators at the Chelsea station of the London Underground Electric Railways Co. are built of "Whitworth Fluid Pressed Steel."

In Chapter IV have been given some notes on the properties of materials used in the construction of the various parts of rotating field systems.

There are two general types of rotating field in use, the one having definite pole pieces with bobbins and coil windings, the other consisting of a smooth core with the windings arranged in slots. A British patent of Swinburne and Crompton (*No. 17,120 of 1886*) describes a rotating field magnet built of laminated sheets with the

winding distributed in slots or wound through holes near the air gap surface. The smooth drum type was employed by C. E. L. Brown in the first turbo-alternators of Messrs. Brown Boveri & Co. and at the present time Messrs. Brown Boveri & Co. use this type in all their machines, from 100 kw. at 3000 R.P.M. to 6000 kw. at 750 R.P.M. Figs. 174, 175, and 176 illustrate forms of Brown's construction as set forth in German Patent No. 138,253 of 1901. The patent claim relates to the subdividing of the field winding into a number of slots with a view to distributing the centrifugal force of the winding, the whole of the field iron being thereby brought into close proximity with the armature face, and the space between the usual salient pole pieces being utilised. It might be thought that such a scheme and the presence of the winding teeth would lead to stray fields and undue magnetic leakage, but this has not proved to be the case. Several varieties of construction are described in C. E. L. Brown's patent, the differences being chiefly in the nature of the slot arrangement. In the case of Fig. 174 the slots are milled parallel to the axis of the magnet poles; in Fig. 175 the slots are radial; they may also have their axis perpendicular to the magnet axis similar to the Westinghouse structure shown in Figs. 185 and 186, or they may consist of a series of tunnel slots, which may sometimes be more suitable for cable windings, as in Fig. 176.

Figs. 174, 175, and 176 show 2-pole systems, but the structures are easily extended to multipolar designs where they work out more satisfactorily owing to the smaller lengths of the embedded parts of the field winding. The open slots may be closed by wedges of a special non-magnetic bronze, with or without an insulating wedge, according to the exciter voltage.

In Brown's construction, the rotor, except in the large sizes, is usually made from a solid piece of steel. The steel forging consists of a solid central block with the ends drawn out to form the shaft. The first operation is to turn into the body a number of cylindrical grooves to form the ventilating ducts. After this, the slots, which are to ultimately contain the field windings, are milled out longitudinally on the surface of the core. Slots are only cut at such places as are required for the windings, and the parts at the centres of the field coils are left unslotted.

This constitution should be compared with that which has been employed by the Oerlikon Company for the rotors which are

described on pages 250 to 254. These are made from stampings, and are slotted all around the periphery. It is advantageous to provide the entire periphery with slots for the purpose of saturating the iron at these points, but when the slots are milled out, the cutting of these slots would be a considerable and needless expense which is inconsiderable when the slots are stamped.

The coils of Messrs. Brown Boveri & Co.'s rotors are shaped on formers and are afterwards assembled in the slots. A finished rotor is shown in Fig. 177.

FIG. 177.—Finished rotor of a Brown Boveri turbo-alternator.

For large sizes Messrs. Brown Boveri & Co. use laminated sheet iron assembled on a steel shaft, the general design being very similar to that employed when the rotor is composed of a solid piece of steel. The end parts of the winding are held in place by bronze cylinders similar to those employed in continuous current armatures. The inside of these covers is well insulated, and the windings press on, when running, by their own centrifugal force. A series of radial wings on the covers provides an air-draught for cooling. The exciting current is led in at two cast-iron slip-rings, which are placed one at each end of the rotor.

Fig. 178 illustrates a solid smooth type construction of Messrs. Lahmeyer. The Oerlikon Company usually employs for all sizes a smooth core type of slotted drum rotor, built from laminations of sheet steel. On page 276 will be found some details of a 1000 kw. machine built by this firm in the year 1901. In the Oerlikon Company's earliest construction the iron core was in the form of a hollow laminated cylinder, and was provided at its periphery with 120 equally spaced closed slots in which the winding, in the form of copper rods, was embedded. A photograph of the rotor is shown in Fig. 179. The individual turns were led through between the shaft and the inner surface of the hollow cylinder after the fashion of a Gramme

FIG. 178.—Finished rotor of Lahmeyer turbo-alternator.

ring armature winding. At four points between each pair of magnet poles, four slots were left without any winding, since windings in the neighbourhood of the magnet axis make only an inappreciable contribution to the magnetic field, and give to the distribution of the intensity of the magnetic field an unfavourable triangular form. Fig. 180 illustrates a rotating armature of a machine which the Oerlikon Co. built at the same time. The winding was carried out as a drum winding, the ends being laid up spirally in barrel fashion. This type of machine has been abandoned in favour of the rotating field type.

In the Oerlikon Company's more recent constructions the sheet iron of the rotor is provided with relatively large, uniformly distrib-

FIG. 179.— An instance of an early Oerlikon type of ring wound field.

uted slots, a few slots in the neighbourhood of the poles remaining unwound. The remaining slots are filled out with spirally formed coils wound on formers and retained in the mouths of the slots by means of metallic wedges. It is evident that the unwound slots may

FIG. 180. — Rotating armature of an early type of 200 kw. polyphase alternator built by the Oerlikon Co.

be filled up with magnetic material, or that, as an alternative plan, the rotor iron need not be stamped out. However, in general, precisely this decrease in the iron cross section, resulting from the removing of the magnetic material of these slots, affords a convenient means to give to the saturation curve of the generator a form offering advantages as regards voltage regulation.

FIG. 181. — Unwound field of 250 kw. Oerlikon alternator.

Fig. 181 shows an unwound rotating field of this Oerlikon type. It is constructed of stampings assembled on a shaft and clamped between two strong end flanges which are formed in one piece with a cylindrical barrel on which the end parts of the field coils are bedded.

FIG. 182. — An Oerlikon 4-pole rotating field wound. (End shields removed.)

FIG. 183. — Finished rotor of bipolar 2-phase 400-kw. 5000-volt 42-cycle 2520-r.p.m. Oerlikon alternator.

In Fig. 182 a similar rotor is seen with the field coils assembled in the slots, but without the cylindrical end covers in place. Fig. 183 depicts a finished rotor of this type. The slots are completely covered in with wedges and the end windings are covered in by the cylindrical shields.

The development of the Oerlikon Company's field construction since the earliest machines, is illustrated, so far as relates to the

FIG. 184. — Laminations for rotating fields of Oerlikon high speed alternators.

stampings, by the group in Fig. 184, which correspond to the three types of machine outlined above.

A similar smooth drum construction has been used for bipolars by the Westinghouse Company. The construction is illustrated in Fig. 185 and resembles one of Brown's constructions. The end connections of the field windings are embedded in milled slots, dispensing with the end covers of Brown's structure. The winding slots are here milled in all round the rotor body at right angles to the pole axis, and the windings are secured by strong key pieces in the mouths of the slots.

With such a construction it is not possible to shape the coils on formers, but it is necessary to wind them by hand. In large sizes the

FIG. 185. — A Westinghouse 2-pole field structure.

Westinghouse Company often use a structure with definite pole pieces, the winding being embedded in milled slots similar to the above. Fig. 186 is a photograph of a rotor for one of the 33-cycle 3500 kw. alternators supplied by the British Westinghouse Co. for the Neasden station of the Metropolitan Railway of London. It is shown with the field windings in place. This rotor is constructed of solid forgings of Whitworth fluid pressed steel, manufactured from best Swedish iron. After the iron has been melted in a Siemens-Martin furnace, it is run into special mould boxes and subjected to hydraulic pressure while in the fluid state. Then it is reheated and forged, giving a thoroughly worked and uniform material. The sections are rough tooled and annealed before final machining.

For the purpose of winding the field, the rotor is mounted on a horizontal rotating table similar to the bed of a horizontal boring

FIG. 186.— A Westinghouse 4-pole field structure.

mill, with the axis of the poles about to be wound standing vertically. The conductor consists of copper strip wound flat in the slots with a layer of paper insulation wound at the same time as the conductor.

The slots are closed with phosphor bronze key bars which secure the winding in the slots. A disadvantageous feature of this type of rotor lies in the fact that it is hand wound. If a breakdown occurs on the field, the complete rotor must be taken out, and if there are not facilities for rewinding the field coil on site, the rotor has to be sent back to the factory.

At each end of the rotor is an "end bell" consisting of two bronze cylinders with radial connecting arms, as shown in section in Fig. 187. These arms churn air in through the end bell and round the field windings by ducts milled in the sides of the pole body at right angles to the shaft, and so out through the stator ventilating ducts.

FIG. 187.— End bell of Westinghouse rotor.

The rotors for the 5500 kw. 4-pole machines at the Lot's Road Generating Station of the London Underground Railways Co., have

a diameter of 172 cm. ($67\frac{5}{8}$ inches) at the pole face, giving a peripheral speed of 90 metres per second (or about 300 feet per second). For bipolar alternators a smooth drum is almost always employed, although it is possible to design a definite pole bipolar structure. The chief difficulties with the latter relate to the mechanical retaining

FIG. 188.—Bipolar laminated field — Westinghouse Co. 300-kva. 3000-r.p.m. 6600-volt 50-cycle 3-phase.

of the large and concentrated masses of field copper, and to effective and permament balancing.

Fig. 188 shows a recent type of bipolar laminated field as built by the Westinghouse Company. The rated output of the machine is 300 kva., and the speed is 3000 R.P.M. It will be noticed that the

FIG. 189. — Turbo-alternator field — Walker compensated type 3-phase 650-kva. 25-cycle 1500-r.p.m.

winding is placed in tunnel slots, and that ventilation is obtained by means of vanes on the end bells.

Figs. 189 and 190 show two laminated fields wound with the Miles Walker Compensating Winding. They are for machines of 650 and

FIG. 190. — Walker compensated type field for 3000-kva. 3-phase 25-cycle 750-r.p.m. 6600-volt alternator.

3000 kva. rated output respectively. Both these rotors are provided with air circulating fans at each end.

Figs. 191, 192, and 193 illustrate constructions which have been employed by the Allgemeine Elektricitäts Gesellschaft for their turbo-alternator rotors. Fig. 191 is a section through the unwound core and winding covers of a 500 kw. 3-phase bipolar alternator.

From this lateral section drawing it will be seen that the shaft and core

FIG. 191 *A*.— Section of coil retaining wedges of A.E.G. rotor.

consist of a single forging with dove-tailed slots milled axially on the surface. These slots serve to retain the toothed pieces which secure the field coils. In Fig. 191 *A* the tooth and wedge construction is clearly shown.

FIG. 191.— 2-pole rotating field for 500-kw. A.E.G. alternator.

Fig. 192 shows the rotor with the field coils placed in position after which the retaining teeth are inserted. Each tooth consists of two main parts which are forced laterally against the sides of the dove-tailed slots by the insertion of a radial strip which is in turn secured by a small key strip at the outer surface of the rotor.

Fig. 192 *A* shows a completed rotor with the end shields in place totally concealing the windings.

FIG. 192.— Rotor of A.E.G. turbo-alternator showing field coils in place.

Figs. 193 and 194 illustrate a smooth drum type construction of the General Electric Co. of America. An interesting feature relates to the means of attachment of the shields for the end windings. The

FIG. 192 *A*.— Complete rotor of A.E.G. turbo-alternator.

outer retaining cylinders are secured to the inner shell by a large number of short studs which relieve the outer shell of part of the stress of the end windings. These are the rotors of the 9000 kva. generators for the New York Edison Co.

A laminated construction with laterally parallel slots has been employed by the Bullock Company in some of their bipolar designs. A section through the end of the rotor showing a method which

FIG. 193.— Smooth core rotor of the American General Electric Co.'s 4-pole 9000 kva. 750 r.p.m. alternator.

FIG. 194.— Finished rotor of the American General Electric Co.'s 4-pole 9000 kva. alternator.

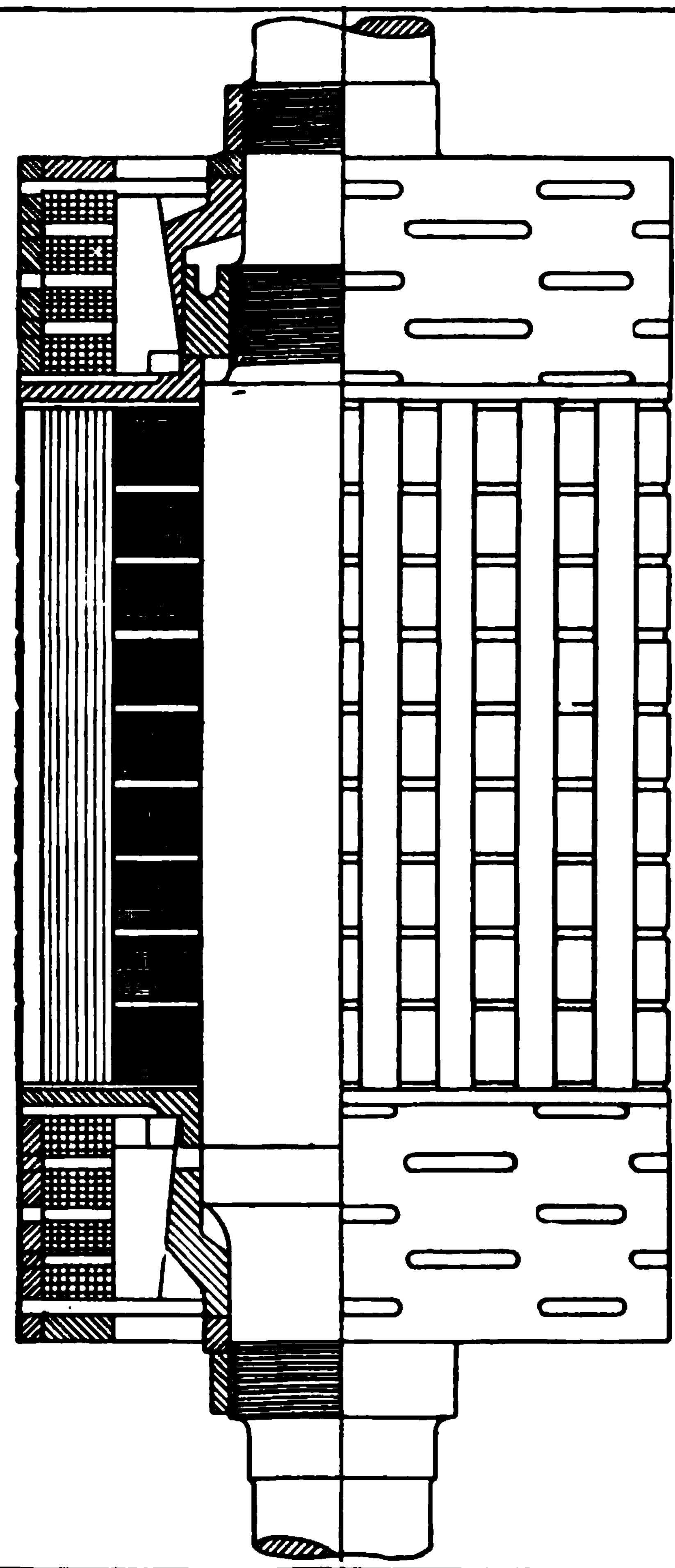


Fig 195. Bullock Co's Smooth Core Rotating Field Construction.

they have employed for retaining the coils is shown in Fig. 195. The end windings are ventilated by means of air passing from the

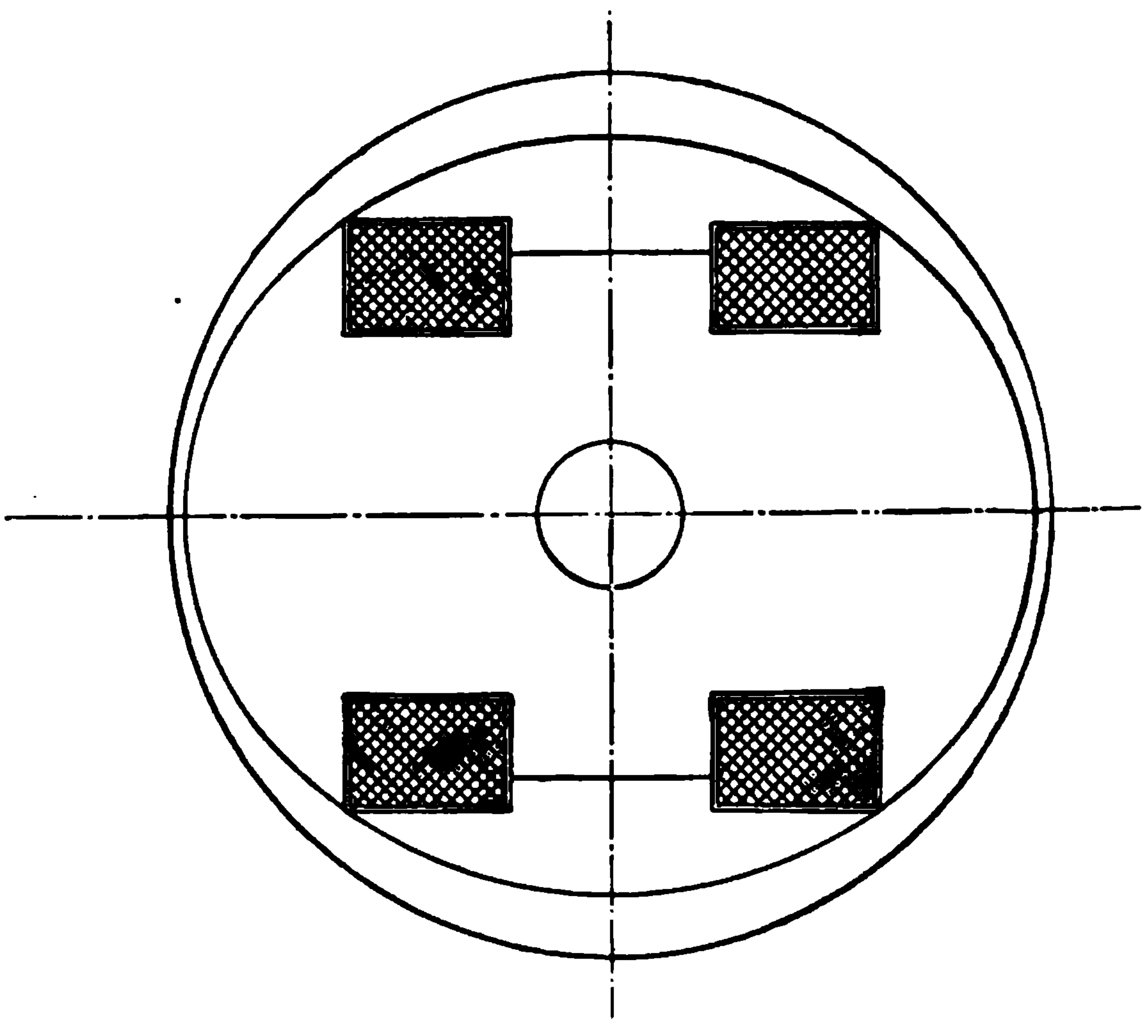


FIG. 196.—A Siemens-Schuckert rotating field for bipolar alternator.

internal surface through the spaces between the coils at their ends, and out through circumferential holes in the end covers. The core body is arranged with radial ducts linking up with air channels

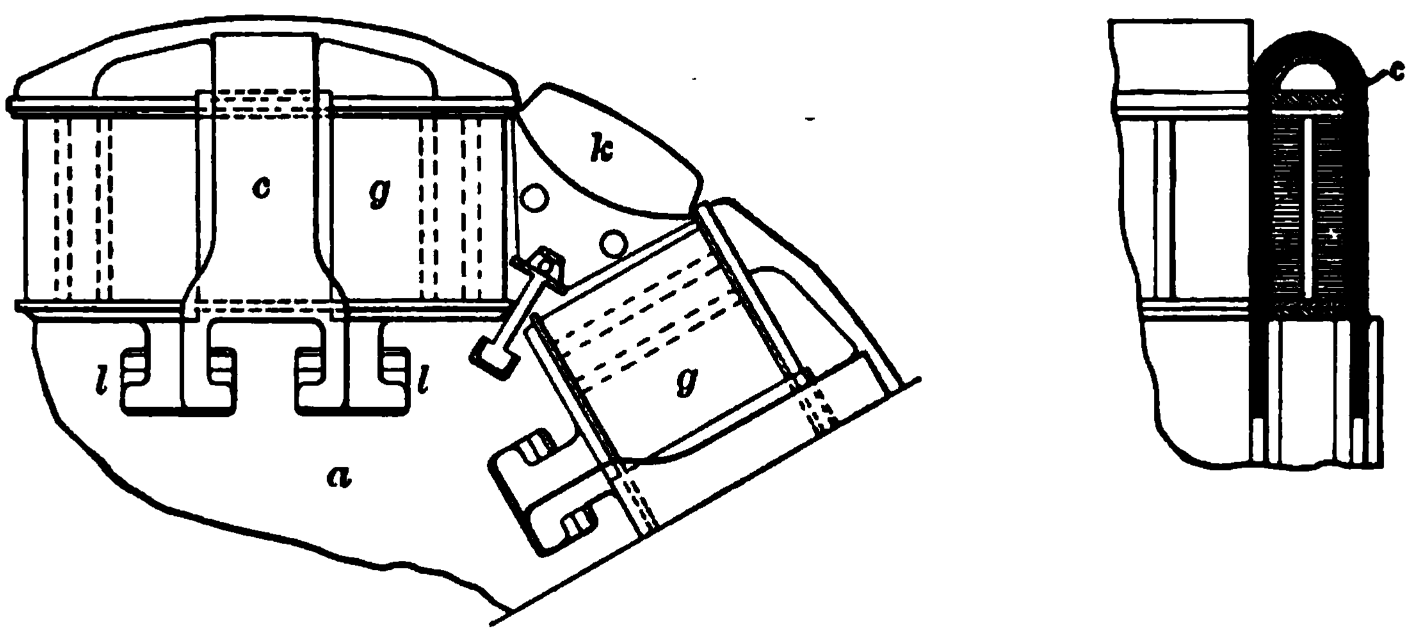


FIG. 197.— A method of clamping field coils. (General Electric Co. of America.)

cut axially near the shaft. This arrangement of end windings should be compared with the Oerlikon structure shown in Fig. 183, page 255.

We have referred above to the possibility of constructing a

2-pole rotor with definite pole pieces and a concentrated field winding. Such a structure would in appearance resemble the old Siemens H-type armature. A recent construction on these lines by the Siemens Schuckert Werke is outlined in Fig. 196. The rotor consists of a central solid hub forming two pole pieces. The field coils, which are not distributed, are retained by bracket pieces shaped to give the rotor a smooth cylindrical surface. The feature of interest

FIG. 198. — Definite pole rotor for 6-pole 5000 kva. 5000 r.p.m. alternator for Twin City Rapid Transit Co. (General Electric Co. of America.)

is that this surface is made to have an elliptical contour instead of a circular contour. This is obtained by arranging the air gap to be of fairly uniform depth under the greatest part of the pole faces. The depth is made sufficient to give the required regulation, but it is gradually widened out at the coil retaining brackets. This arrangement is made to facilitate the entry of air for ventilation at the gap surface.

DEFINITE POLE FIELD STRUCTURES.

The constructions already described have the poles and yokes all in one piece. The field winding is subdivided into slots, and its centrifugal force is distributed amongst a number of projecting ribs or teeth.

Other arrangements already in use comprise a solid hub with detachable pole pieces, or, as an alternative, a solid hub and poles in one, with detachable pole shoes. In these cases the winding may be carried out on a bobbin slipped on the poles in the ordinary way, but special arrangements have to be made to take up the large centrifugal force of the concentrated masses of copper. The stresses in various parts of the rotor, are dealt with in Chapter XII. We shall now describe some constructions of this nature.

FIGS. 199 *A* and *B*.— Angle brackets for retaining field coils.

FIG. 200.— Subdivided field coil.

Fig. 197 illustrates a rotor construction which has been employed by the General Electric Company of America and by the British Thomson-Houston Company. This construction comprises a laminated hub, *a*, with axial slots, *l*, into which projections on the laminated pole pieces fit, and are secured by square wedges. The field coils, *g*, are held by angle brackets, *k*, between the poles, and the end portions of the coils are secured by metal straps, *cc*, held by the wedges which secure the pole pieces.

A photograph and drawings of a complete rotor on these lines is shown in Fig. 198 which relates to a 5000 kva. 6-pole 1000 R.P.M.

three-phase alternator built for the Twin City Rapid Transit Co. by the General Electric Co.

Complete drawings showing the constructional details of a similar rotor will be found in Fig. 220 on page 288. The angle brackets k (Fig. 197) are necessary to counteract the lateral component (at right angles to the axis of the pole) of the centrifugal force of the side portions of the field coils. In Fig. 197 these brackets are retained by a number of bolts whose heads are recessed into a channel formed at the corners of the hub stampings.

Other methods of attaching these brackets are shown in Figs. 199 *A* and *B*. If the centrifugal force of the field windings is too great to be sustained by the pole shoe, the method shown in

Fig. 201. — 4-pole rotating field for 1500 kw. 1500 r.p.m. Bruce Peebles alternator.

Fig. 200 may be employed. Here the coil is subdivided into three parts, the centrifugal force of each part being transmitted to the pole body by means of a ring or pin sunk into a channel in the pole body.

Messrs. Bruce Peebles & Co. employ a solid steel casting for their 4-pole rotating fields. The steel is cast under pressure to ensure homogeneity and absence of blow-holes. A complete 4-pole Bruce Peebles rotor for a 1500 kw. 1500 R.P.M. alternator is shown in Fig. 201. The field coils are wound flat on the pole pieces, while the main casting is held in a lathe and revolved about the axis of the poles. The ends of the field coils are held in place by an extension of the pole shoe which is cast solid with the main casting. The angle brackets are of gun metal, and are bolted on to the hub by special manganese steel bolts which are locked to prevent any shifting.

FIG. 202. — General arrangement of 3000 kw. Dick Kerr turbo-alternator.

In a construction by Messrs. Dick Kerr & Co., the angle brackets are not secured to the hub. Fig. 202 shows the general arrangement of a Dick Kerr alternator.

The rotor consists of a central casting comprising the hub and pole pieces with the pole shoes detachable and dovetailed into the pole cores. The field coil angle brackets are in two halves hinged together at their lower ends. They are retained by the overhanging pole tips, and kept rigid by adjustable bolts as shown.

FIG. 203. — Complete Rotor of a Dick Kerr turbo-alternator.

Fig. 203 shows a general view of a complete rotor. In such a construction the entire centrifugal force of the field coils has to be sustained by the pole shoes, as the angle brackets are not anchored to the hub as in Fig. 201.

Fig. 204, *a* to *l*, illustrates a variety of methods of attaching pole shoes and pole cores suitable for use in high speed designs. *a*, *b*, and *c* are more applicable to moderate speeds where the diameter is sufficiently large to admit of a cast steel hub with a rim through which bolts can pass to secure the poles. *d* to *g* are adaptable to hubs fitting direct on the shaft, being either solid, laminated, or built up of thick plates. *e* is a method frequently employed, in which the root may consist of a single dove-tail, as shown, or may be divided into two if the stresses would otherwise be too great. *f* is suitable for

laminated poles and hubs, the alternate stampings being staggered and secured by parallel wedges. *g* is for laminated poles and hubs, and has already been referred to as one of the constructions em-

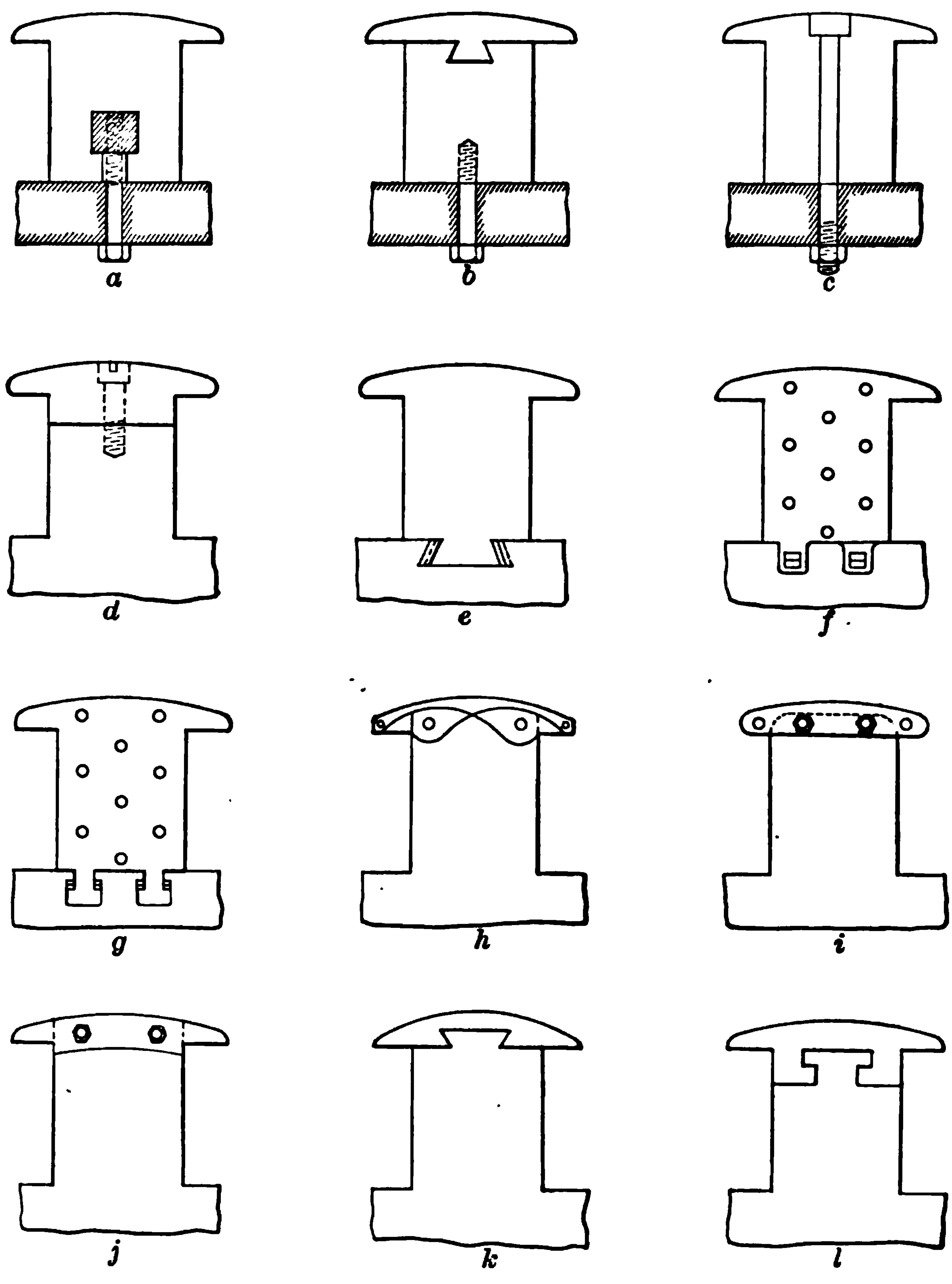


Fig. 204.— Methods of attaching pole cores and pole shoes.

ployed by the American General Electric Company. *h* is a type suitable for 4-pole machines where the diameter is not large. The hub and pole pieces are built from plates in the shape of a cross, and

the pole shoes are secured as shown, alternate stampings being staggered and secured by bars passing through holes in the pole face; *i* and *j* are constructions with solid poles, in one piece with the hub, with laminated shoes let into grooves in the pole face. In *i* these grooves are milled flat, and in *j* they are turned concentric with the pole face, the two operations being performed at one setting in the lathe.

Figs. *k* and *l* relate to solid poles and hubs with a detachable shoe which may be solid or laminated; with poles of large size and weight it is preferable to recess the dove-tail into the shoe as in Fig. *k*, and not into the pole as in Fig. *l*. In the latter case the centrifugal

FIG. 205. — General arrangement of 4000 kw. 3-phase 400 r.p.m. 60 cycle 2400 volt alternator designed by Rushmore.

force of the shoe itself is greater, due to its extra weight, and this stress has to be borne by the section at the root of the dove-tail. Fig. *l* has been employed by Messrs. Parsons and by Messrs. Dick Kerr & Co.

All the constructions shown in Fig. 204 permit of employing field coils wound on formers and are slipped into place on the field poles after winding. These methods are all very serviceable, and each one presents advantages peculiar to itself rendering it applicable in certain cases.

Field Constructions for Water Wheel Generators. — In the case of generators driven by water turbines as the prime mover, the average range of speeds is not so high as that for steam turbines. The speed

does not usually exceed 400 to 600 R.P.M., and thus water wheel alternators will have a number of poles ranging from 8 to 20.

The normal speed, however, is not the maximum speed at which the generator is liable to run, but it is subject to a speed about twice normal speed, as water wheels usually operate at a speed which gives the rim a peripheral speed equal to one half the spouting velocity of the water.

FIG. 206.— Assembled field core of 4000 kw. alternator, designed by Rushmore.

The field has therefore to be designed with sufficient mechanical strength to provide against the stresses at such high speeds as 100 per cent over normal speed. A generator having a normal peripheral speed of 40 metres per second may have to withstand the stresses which would occur at a speed of 80 metres per second. For this reason it is desirable not to employ cast rims for the magnet yoke, but to use some form of laminated or forged construction.

A good design by D. B. Rushmore* is shown in Figs. 205, 206 and 207. The machine is a 4000 kw., 400 R.P.M., 60-cycle 2400-volt 3-phase generator. The rim of the revolving part is built from sheets of iron one eighth inch thick. A single section of the rim comprises

* See "The Mechanical Construction of Alternators," D. B. Rushmore, Proc. American Institute Electrical Engineers, April 22, 1904.

two poles, and the part between them as is seen in Fig. 205. The laminations are dove-tailed into steel ribs which run parallel to the shaft and are riveted into the end plates. The whole mass of pole and yoke laminations is riveted together by long rivets passing through the end plates and securing the poles to them as shown in Fig. 206.

The field coils are wound on bobbins and are secured to the poles by the pole shoes. The latter are laminated and fit in grooves formed

FIG. 207.— Wound field of 4000 kw. alternator, designed by Rushmore.

on the pole face by arranging the alternate pole stampings shorter and longer, in a similar manner to that shown in Fig. 204 *i*.

The bolts which pass through the pole shoes to secure them, project at either end so as to retain the end parts of the field coils by clamping the phosphor bronze brackets. Three angle brackets are provided between each pair of poles and take up the lateral component of the centrifugal force of the coils which would tend to make them bulge. These brackets are held in place by straps passing over them and down between the laminations of the field ring, below which a rod is run through a hole in their ends as shown in Fig. 207.

With such a built-up plate construction for the pole system, the coils may be retained by a wedge piece engaging in two slots on the side of the pole near the face. Such an arrangement is shown in

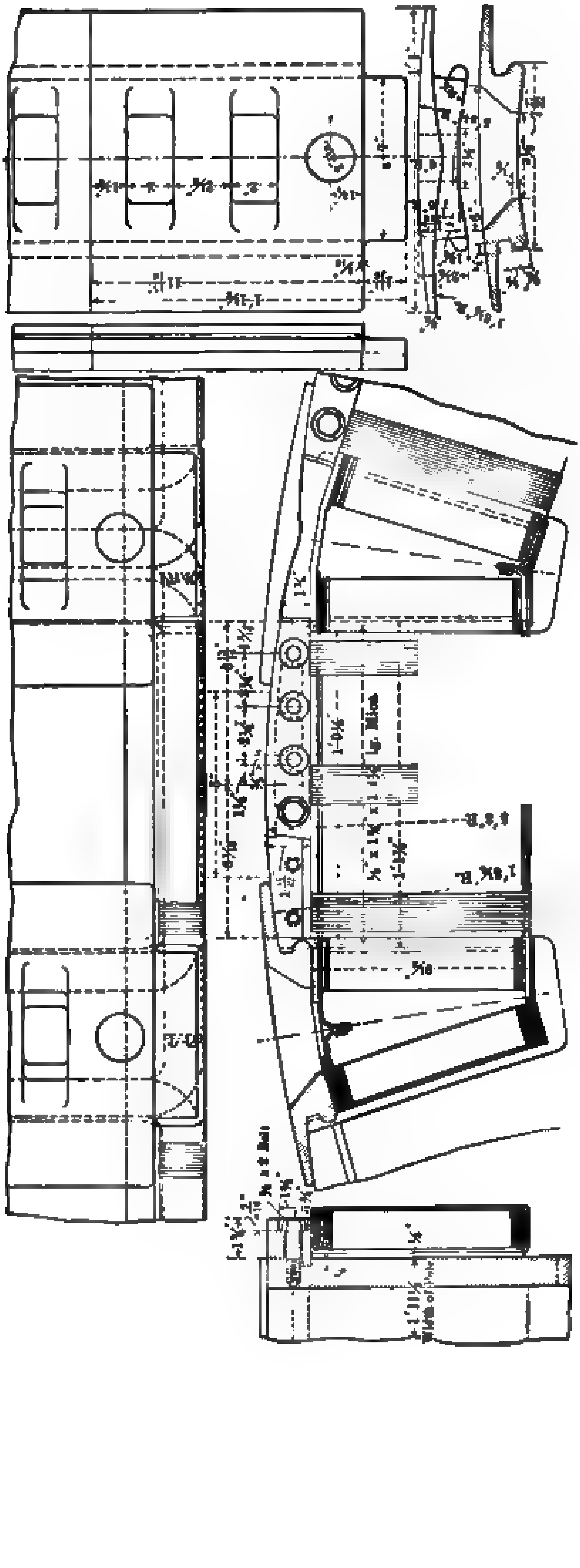


FIG. 208. — Field coil retaining pieces for Westinghouse 3750 kw. 20-pole 3-phase 2200 volt, 30 cycle 1000 r.p.m. alternator.

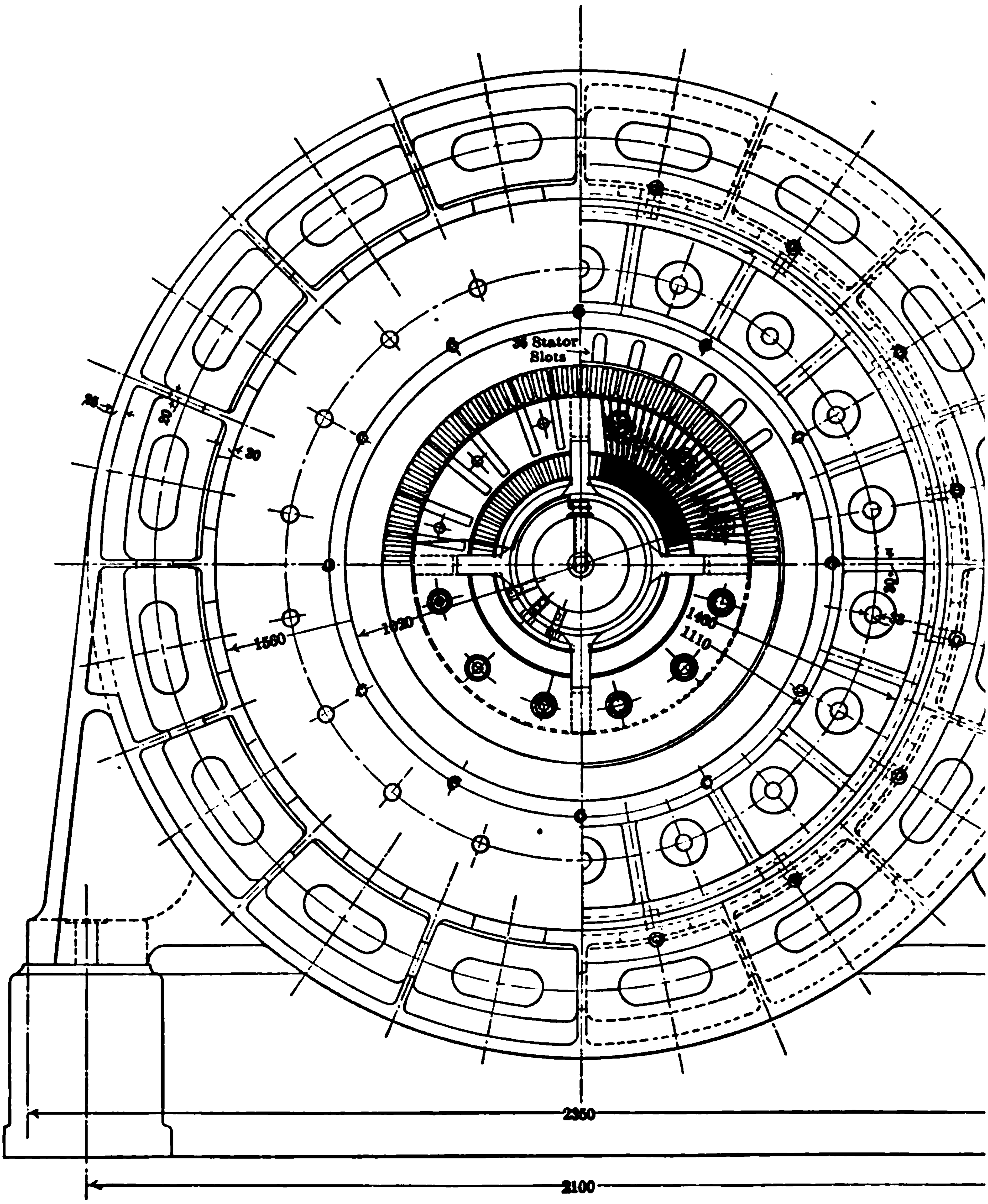
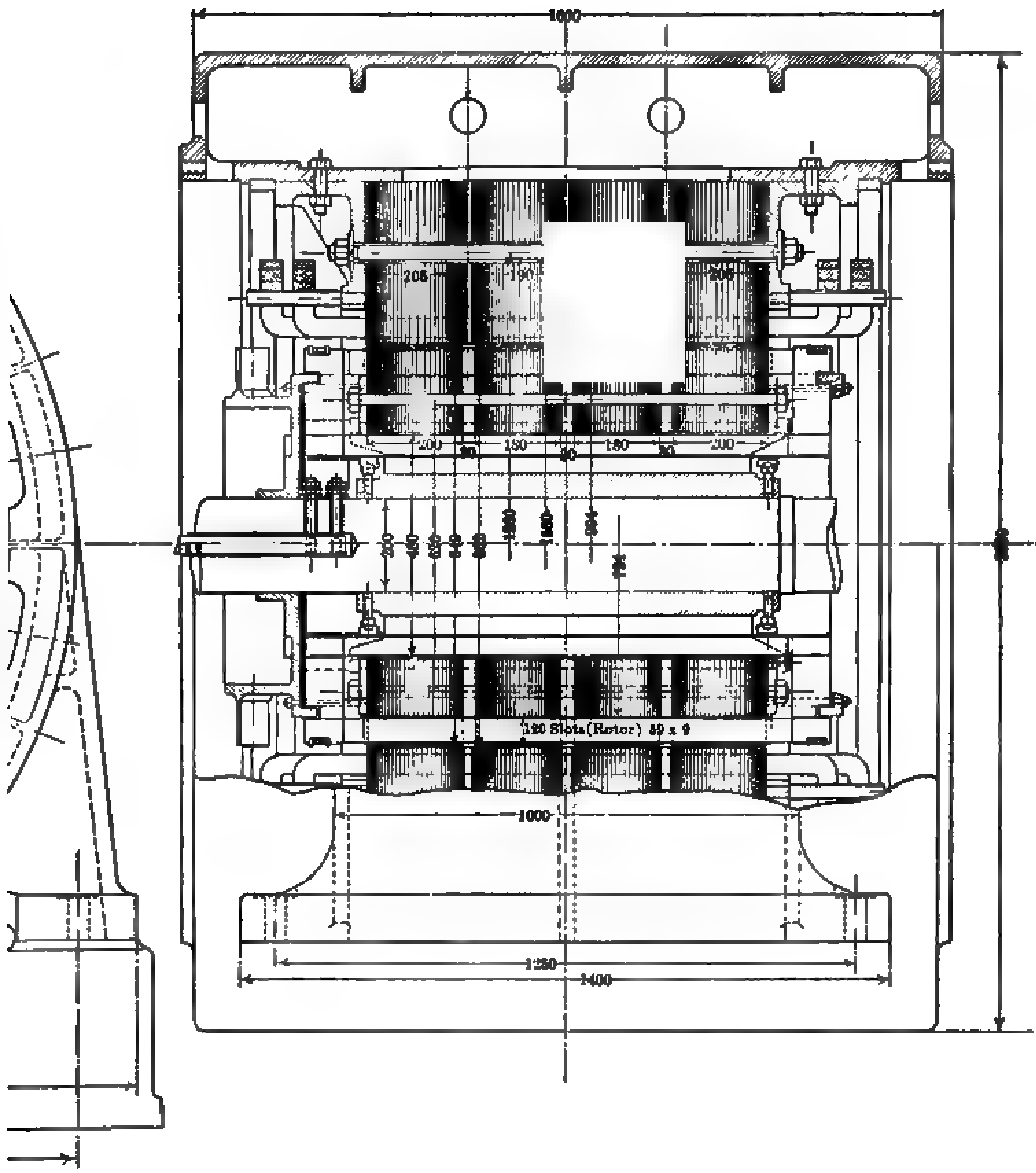


Fig. 211.— General arrangement of 1200 kva. 3-phase 150



·.p.m. 4-pole 50 cycle 1155–2000 volt Oerlikon turbo-alternator.

Fig. 208, which relates to a 3750 kw. 20-pole 3-phase 2200 volt 30 cycle 100 r.p.m. Westinghouse alternator. A single field stamping is given in Fig. 208, and Figs. 209 and 210 show the manner in which the pole stampings are cut from the sheet.

Some advantages claimed for the smooth drum construction for rotating fields of turbo-alternators relate to the diminution of the losses by air friction, and to the more noiseless running.

The first of these advantages is undoubtedly secured, but on the other hand with a continuous drum revolving within and close to

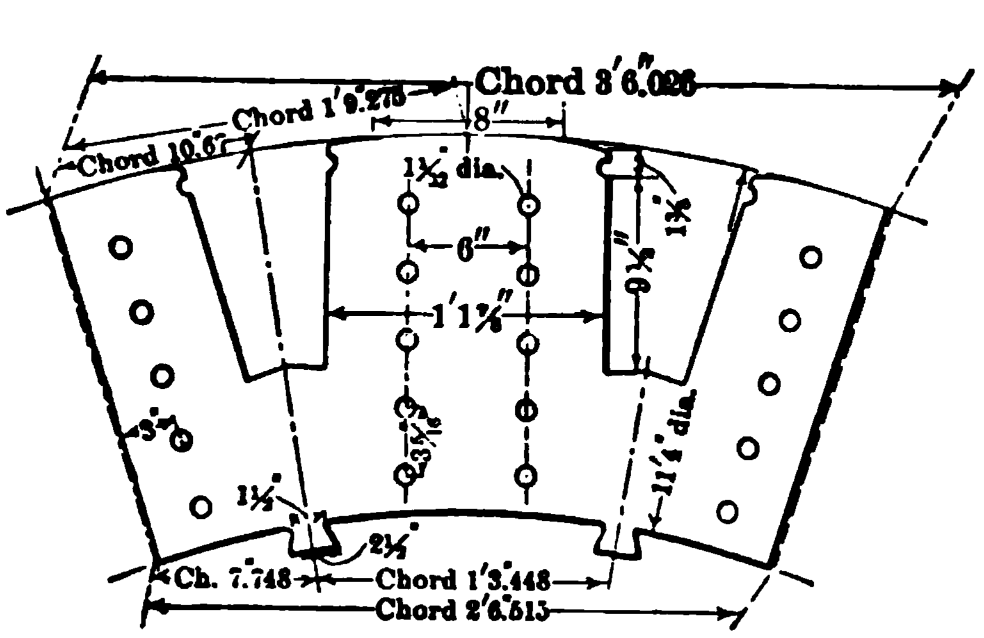


Fig. 209.

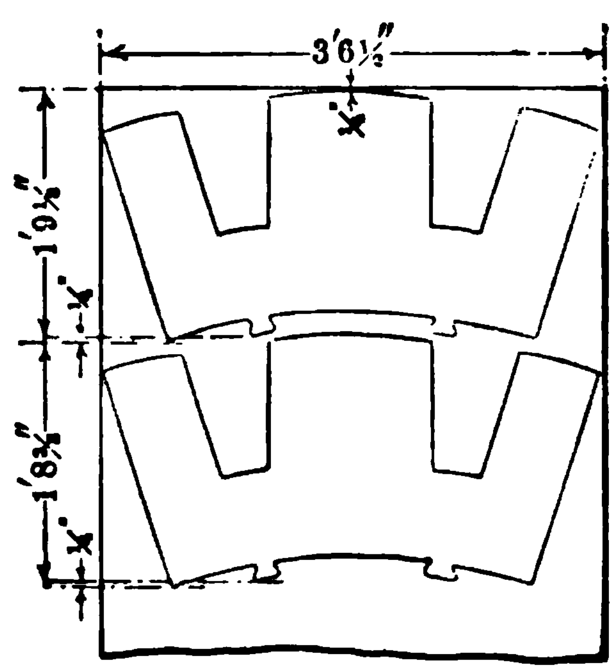


Fig. 210.

Figs. 209 and 210.— Field magnet stampings of Westinghouse 3750 kw. 20-pole alternator.

the stator the ventilating properties are not so good as with the definite pole construction with spaces between the poles.

On the second point the smooth construction has the advantage: the sudden variations in the sections of the currents of air caused by projections or irregularity in the shape of the rotating element augment the hum associated with high speed machines. From this standpoint the rotor should be designed to present to the air as smooth surfaces as possible.

The definite pole construction has, however, been successfully employed in large sizes by several makers. In large rotors the solid construction is preferable to a built-up structure, on the score of simplicity and safety, and also because it presents less liability to unbalancing troubles. The question of balancing is, of course, of the utmost importance. The component parts of the rotor should be balanced individually and afterwards the whole assembly, both for static and for running balance.

For the application of balance weights, dove-tail or channel shaped grooves are turned on some solid part of the rotor at as large a radius as practicable, the necessary weight required to effect a balance being thus reduced.

EXAMPLES OF TURBO-ALTERNATORS.

Turbo-Alternators of the Oerlikon Company.*

When in the year 1900 the Oerlikon Company undertook the construction of steam turbines with alternating current generators, three generators of three different types were at first put in hand. These were respectively of the inductor type, the rotating armature type and the rotating field type. The actual machines put in hand were:

1. A polyphase generator for an output of 1200 kilovolt amperes at 1500 R.P.M. and 50 cycles. This generator was of the inductor type in which the rotating part has no windings upon it.
2. A quarter phase generator for an output of 600 kilovolt amperes at 2520 R.P.M. and 42 cycles per second with a rotating armature and external stationary field magnets.
3. A polyphase generator for 1200 kilovolt amperes at 1500 R.P.M. and 50 cycles with a rotating field wound after the fashion of a continuous current armature, that is, having the field windings distributed in slots, and with an external stationary armature.

The two first systems were shortly afterwards given up; to-day the alternating current generators of the Oerlikon Company are built only in accordance with the third system, that is, of the rotating field type only, and, moreover, the rotating fields are all of the cylindrical construction as distinguished from definite pole constructions.

The construction and putting in service of these generators occurred toward the end of the year 1901. Fig. 211 is an outline drawing of the third machine, to which the following data relates:

Rated output	1000 kw. — 1200 kva.
Terminal voltage	2000
Number of phases	3

* Further details of construction of the Oerlikon Company's high speed alternators will be found on pp. 251 to 255 of this chapter.

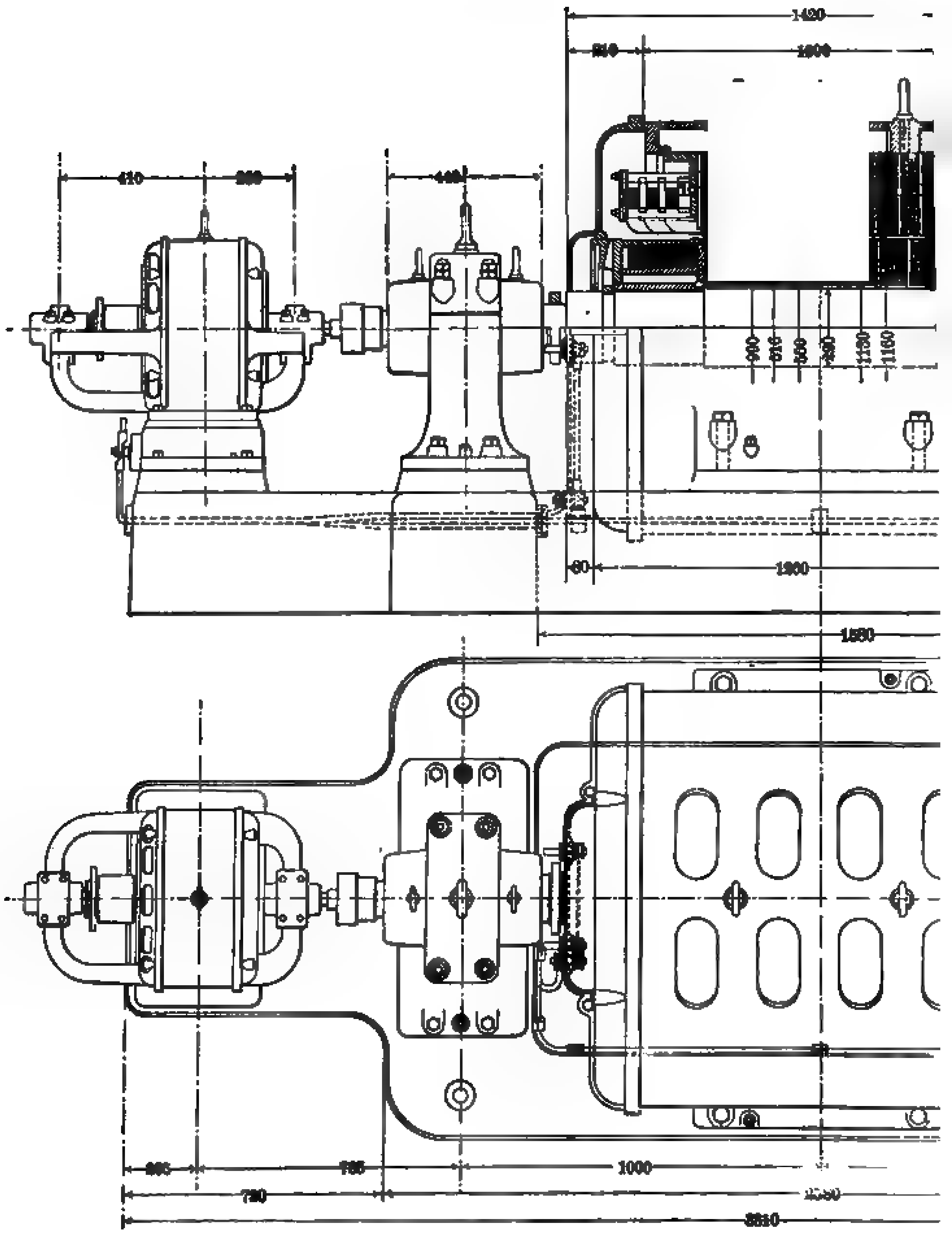


FIG. 212.— General arrangement of 250 kva. 3-phas

)00 r.p.m. 2-pole 50 cycle Oerlikon turbo-alternator.

Volts per phase	1155
Frequency	50
Speed R.P.M.	1500
Number of poles	4

DIMENSIONS IN MILLIMETRES

Air gap diameter of stator (D)	862
External diameter of stator	1550
Gross length of armature core (λ_g)	850
Number of ventilating ducts	3
Width of each duct	32
Net length of laminations (λ_n)	680
Polar pitch at air gap (τ)	676
Output coefficient (ξ)	1,27
Ratio $\frac{\lambda_n}{\lambda_g}$	0.8
Radial length of air gap	6.5
Ratio $\frac{\lambda_g}{\tau}$	1.25
Number of stator slots	36
Number of rotor slots	120
Stator slot pitch	75
Rotor slot pitch	19

4 conductors per slot corresponds to 24 turns per phase, and to a flux of 22 megalines per pole.

The general construction of the rotating field for this type of machine has been fully described on pages 250 to 254.

Data for an Oerlikon 250 *kw.* 3-*Phase* 3000 *R.P.M.* 2-*Pole* 50-*Cycle Alternator.*

An assembly drawing of this machine is shown in Fig. 212, and the following is a short specification and analysis of the design:

Output in kva.	250
Number of phases	3
Frequency	50
Speed in R.P.M	3000
Output coefficient	0.000467
Peripheral speed in metres per second	81
Diameter at air gap (D)	516
External diameter	990
Gross axial length of core (λ_g)	670
Number of ventilating ducts in core	10
Width of each duct	8
Effective core length (λ_n)	530

Ratio $\frac{\lambda_n}{\lambda_g}$	0.79
Number of slots	36
Slot pitch at armature face	46.3
Depth of slot	35
Depth of iron above slot	202
Ratio $\frac{\lambda_g}{\tau}$	0.85
Number of slots per pole per phase	6

FIELD MAGNET SYSTEM

Number of poles	2
Radial length of air gap	8
Diameter at pole face	500
Pole pitch (τ)	785
Number of slots	48
Slot pitch at face	32.7
Conductors per slot	1
Rotor slots per pole	20
Axial length of field core	640
Number of ventilating ducts	7
Width of each duct	10
Net axial length	513
Depth of field laminations	130

The stator winding is whole coiled and laid up in three ranges. There are thus 6 coils, 2 in each range.

The field winding is thoroughly distributed in radial slots, the end portions of the coils being retained by cylindrical end shields. The latter are perforated with holes near the shaft thus admitting of air circulation along the shaft and up the ducts in the rotor core.

Data for an Oerlikon 1000 *kva.* 1500 *R.P.M.* 4-*Pole* 50-*Cycle* 5200-*Volt Single-Phase Alternator.*

This machine is illustrated in Fig. 213, and drawings are given in Figs. 214 and 215.

The following is a specification and analysis of the design:

Dimensions in Millimetres.

Rated output at unity power factor	1000
Number of phases	1
Periodicity in cycles per second	50
Speed in revolutions per minute	1500
Number of poles	4
Terminal voltage	5200

FIG. 213. — 1000 kva. 5200 volt 1500 r.p.m. Oerlikon single-phase turbo-generator.

Current per terminal	193
Voltage per phase	5200
Armature Iron	
Internal diameter of laminations (at air gap) (D)	870
External diameter of laminations	1500
Gross length of core between flanges (λg)	1010
Number of ventilating ducts	12
Width of each duct	10
Effective length of core (iron) (λn)	800
Number of slots	48
Depth of slot	70
Width of slot	28.3
Rotating Field	
External diameter of laminations (at air gap)	840
Internal diameter of laminations	380
Gross length of core between flanges	950
Number of ventilating ducts	11
Width of each duct	10
Effective length of core (iron)	756
Number of slots	32
Depth of slot	116
Width of slot	35.6
Armature Winding	
Number of slots per pole, wound	10
Number of conductors per slot	8
Total number of face conductors	160
Number of turns in series per phase	80
Number of wires in parallel per conductor	8
Diameter of each wire	4.4
Effective cross section of conductor	1.22
Current density	158
Arrangement of wires in slot	4×8
Resistance at 20° C.	0.05
Space factor of slot	0.12
Field Winding	
Number of slots per pole wound,	6
Number of conductors per slot	44
Turns in series per pole	132
Nature of conductor	Strip
Dimensions of conductor, bare	3×13
Effective cross section of conductor	0.39
Total resistance of field at 20° C.	0.9
Space factor of slot	0.415

Magnetic Data

Flux in armature (megalines)	39
Dispersion coefficient	1.25
Flux in field	46
Corresponding average flux densities in lines per sq. cm.	
Armature core	9400
Armature teeth	14,000
Air gap	5300
Field teeth	22,000

Weights and Costs

	Kilograms	*Dollars*
Armature laminations	6650	820
Field laminations	1900	240
Effective iron, total	8550	1060
Armature copper	400	200
Field copper	800	400
Effective copper, total	1200	600
Total effective material	9750	1660
Effective material per kva. output	9.75	1.66

Efficiency (per cent)

Full load	97
Half load	96
Quarter load	93

Constants and Coefficients

Output coefficient (ξ)	0.865
Ampere conductors per centimetre (α)	112
Flux per square centimetre (β)	5300
Peripheral speed (S)	66
Ratio $\frac{\lambda_g}{\tau}$	1.49
Ratio $\frac{D}{\lambda_g}$	0.86
Ratio of field ampere turns to armature ampere turns	1.9

Stator. — The frame consists of a cast iron shell divided across the centre line of the machine. The laminations are built up on four ribs cast radially inwards from the frame, and are clamped between substantial end plates by means of bolts passing through holes near the outer periphery of the laminations. Twelve ducts are provided in the armature laminations in addition to the two end ducts, and the great depth of the ribs on the frame provides a channel for the

air, which is drawn through apertures in the bed plate, into the machine, and is expelled from a series of apertures in the top of the machine.

The laminations, which are in twelve sections, are secured by feather keys, each of which is fastened to the frame by nine bolts. The slots are wide open with grooves in the teeth to fix the wedges for securing the armature winding.

The winding consists of one coil per pole in five sections, leaving eight of the forty-eight slots unwound, and is secured to the frame by brackets of strip iron bolted to the end flanges.

Rotor. — The rotor consists of laminations keyed directly upon the shaft, and held together by stout end plates. For ventilation, there are provided eleven ducts, which are equally spaced between the ducts in the stator laminations. The air is led to these ducts through twelve longitudinal holes in the laminations, each hole being 5 centimetres diameter. There are 32 radial slots which are also of the open type, and the V-grooves for the wedges are specially large to resist the heavy stresses due to the centrifugal force on the winding. Only 24 of the slots are used for the winding. The end portions of the coils are completely enclosed in heavy bronze cylindrical covers.

The exciter for this machine is shown on an extension of the main shaft, and the slip rings for conveying the current to the rotor are shown, one on either side of the cover plates fixed to the ends of the stator to protect the windings.

Fig. 213 gives a view of this machine, and Figs. 214 and 215 show the leading dimensions.

In Fig. 216 is shown the no load saturation curve and short circuit characteristic for this machine; the point shown on the normal voltage line gives the experimentally observed excitation required for full load at unity power factor, and from this we can obtain the corresponding regulation.

The same excitation on open circuit would produce 5650 volts, hence the drop in volts is equal to 5650–5200 or 450 volts; hence the regulation at full load and unity power factor is equal to

$$\frac{450}{5200} \times 100 = 8.65 \text{ per cent.}$$

From the short circuit curve which is shown in the same figure, we can obtain some idea of the ratio of the field ampere turns to the armature ampere turns.

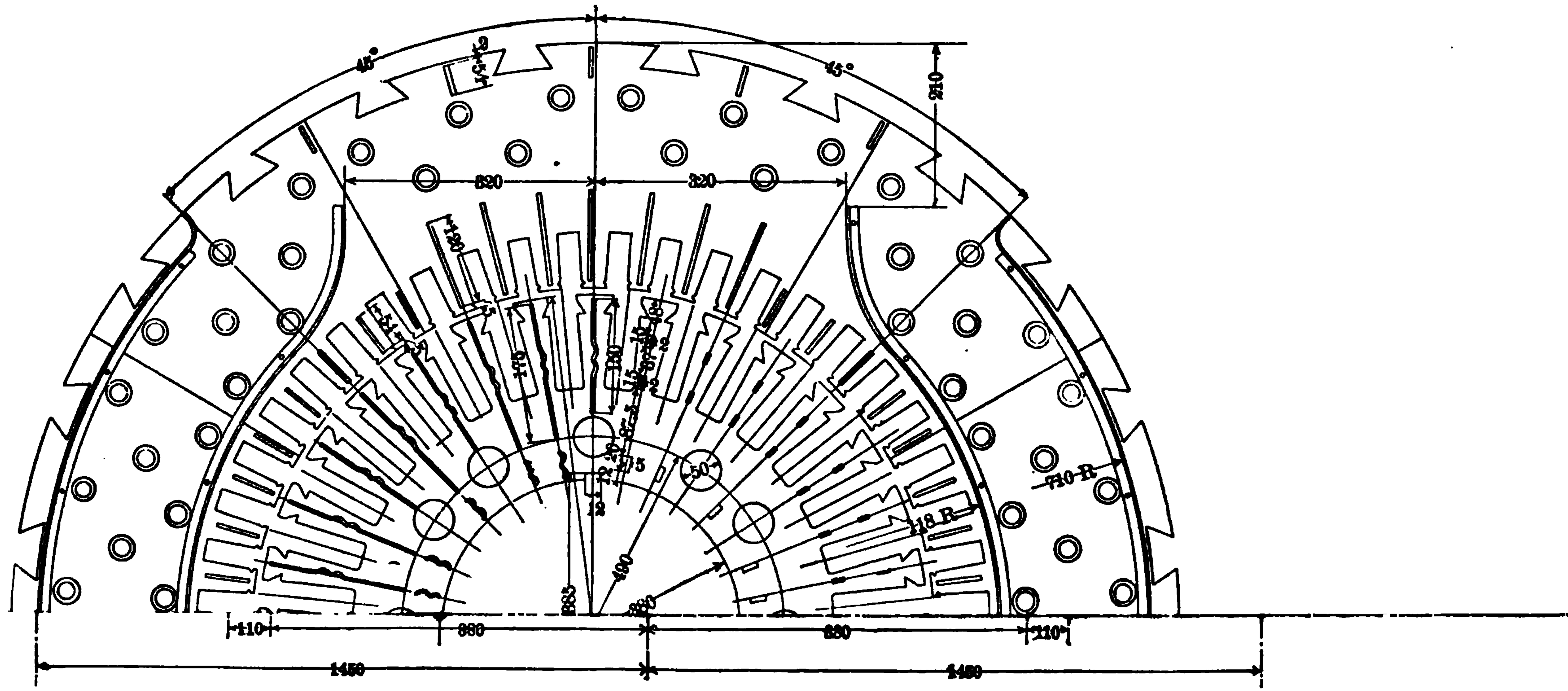


FIG. 214.— General arrangement of 1000 kva. 1500 r.m.p. 4-pole 50 cycle 5200 volt single phase Oerlikon turbo-alternator.

The armature demagnetisation on short circuit is about 0.71 of the armature ampere turns per pole and hence the ratio of the field ampere turns to the armature ampere turns is $0.71 \times 2.64 = 1.87$.

The ratio is actually 1.9 as noted on page 281, which is in agreement with the value of the short circuit current.

Turbo-Alternators of Brown Boveri & Co.

Messrs. Brown Boveri were among the first to devote attention to the special problems of manufacture of generators for steam turbines.

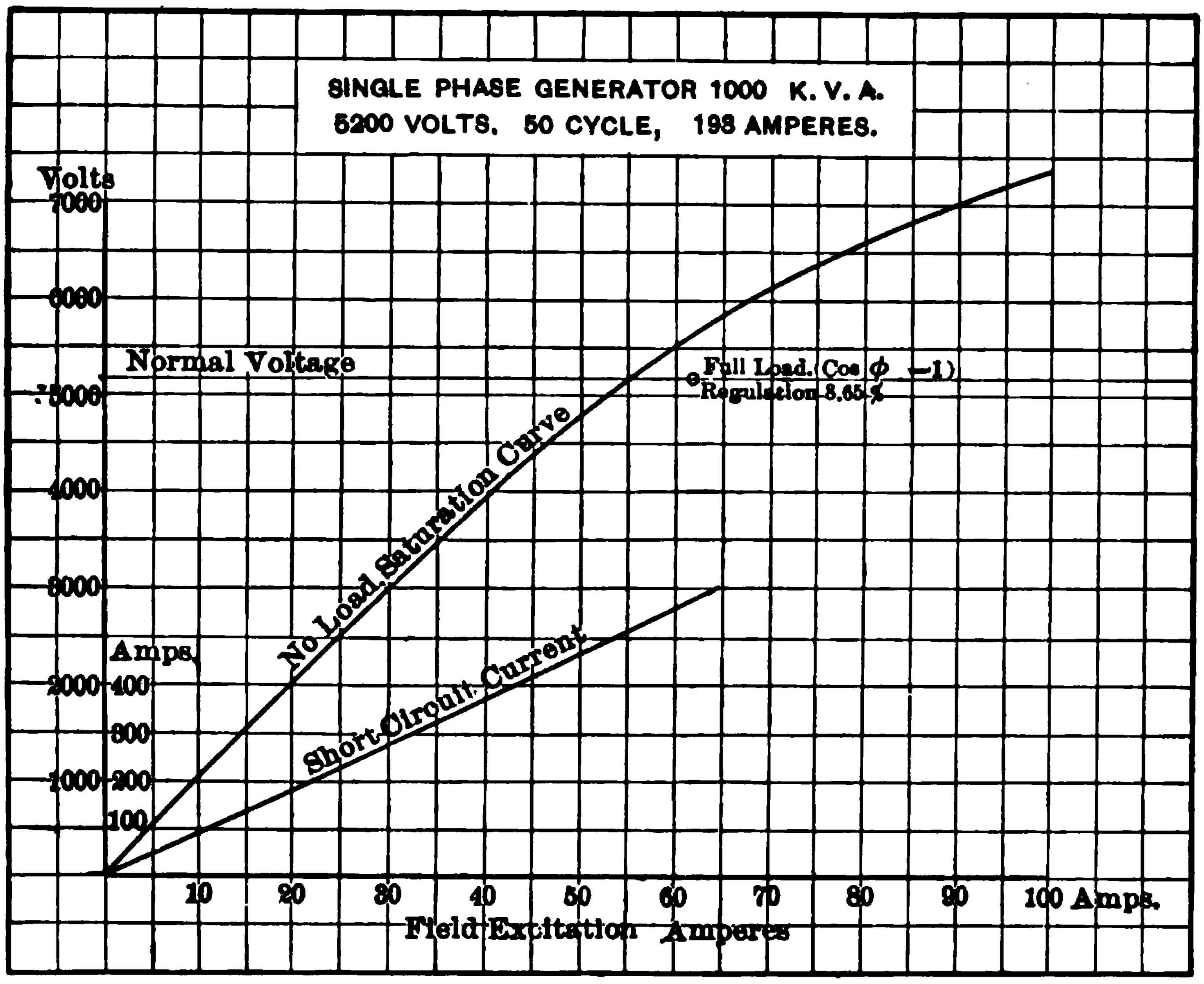


FIG. 216.— Characteristic curves of 1000 kva. 5200 volt 50-cycle single phase Oerlikon alternator.

They have standardised their designs for turbo-alternators, and have even gone so far as to standardise a complete line of continuous current turbo-dynamos up to 1250 kw. rated output. The continuous current turbo-dynamo designs are described in Chapter XVIII. Up to the end of 1906 Messrs. Brown Boveri and Co. had supplied over 500 turbine sets with an aggregate horsepower of over 800,000. Of these the number of turbo-alternators was over 300 with an aggregate horsepower of over 625,000.

The standard turbo-alternators are of the forced ventilation type, with air entries at the base. The rotors are of smooth cylindrical construction with field windings distributed in slots. In the largest sizes the rotors are of laminated construction, but in the majority of cases the rotor is made from a solid piece of steel.

We have already in this chapter given considerable attention to the various details of construction of Messrs. Brown Boveri's machines. On page 224 will be found matter and drawings relating to the frame construction and ventilating scheme. On page 245 the armature windings are referred to, and on page 248 the rotor construction.

In Fig. 217 is given an outline drawing of a standard 1000 kva. 1500 R.P.M. 2000-volt 3-phase turbo-alternator as constructed by Messrs. Brown Boveri and Co.

Turbo-Alternators of the General Electric Co., U.S.A., and the British Thomson-Houston Co.

Description of a 3-Phase, 1500 Kva. 11,000-Volt, 6-Pole, 50-Cycle, 1000 Revolutions per Minute, Alternator.

This machine is one of several installed in the Thornhill Power Station of the Yorkshire Electric Power Company. They are each direct-coupled to a Curtis steam turbine, the complete sets being built and supplied by the British Thomson-Houston Company, through whose courtesy we are enabled to publish this description of the design.

The generators are mounted on top of the turbine, the rotating field magnets being carried on the upper end of the vertical shaft which runs on a hydraulic footstep-bearing supplied with water at 400 pounds per square inch. Fig. 218 shows the assembly of one of these machines, half in section and half in elevation, and Fig. 223 is a photograph of a complete set.

The stator casing consists of an outer shell of cast iron, one inch thick, provided with 18 radial ribs, against which bed the armature laminations, these radial ribs being webbed together with three circumferential ribs of the same thickness, as shown in Fig. 219.

The laminations are secured by feather keys fitting into dove-tail slots in the stampings, and parallel slots milled in the radial ribs. Fig. 219*A* shows the details of the laminations, and indicates the method of holding the space blocks for the ventilating ducts. Insu-

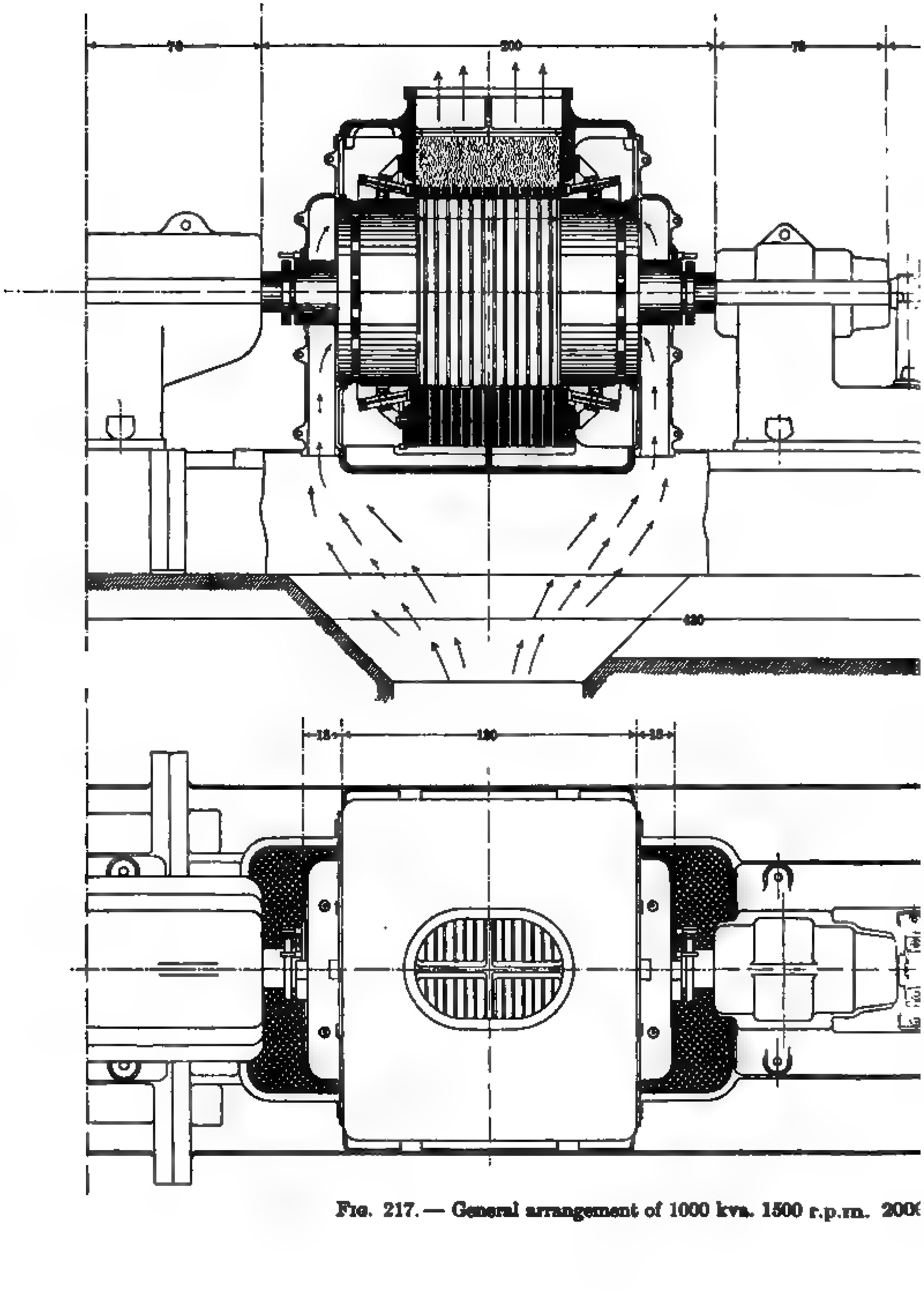
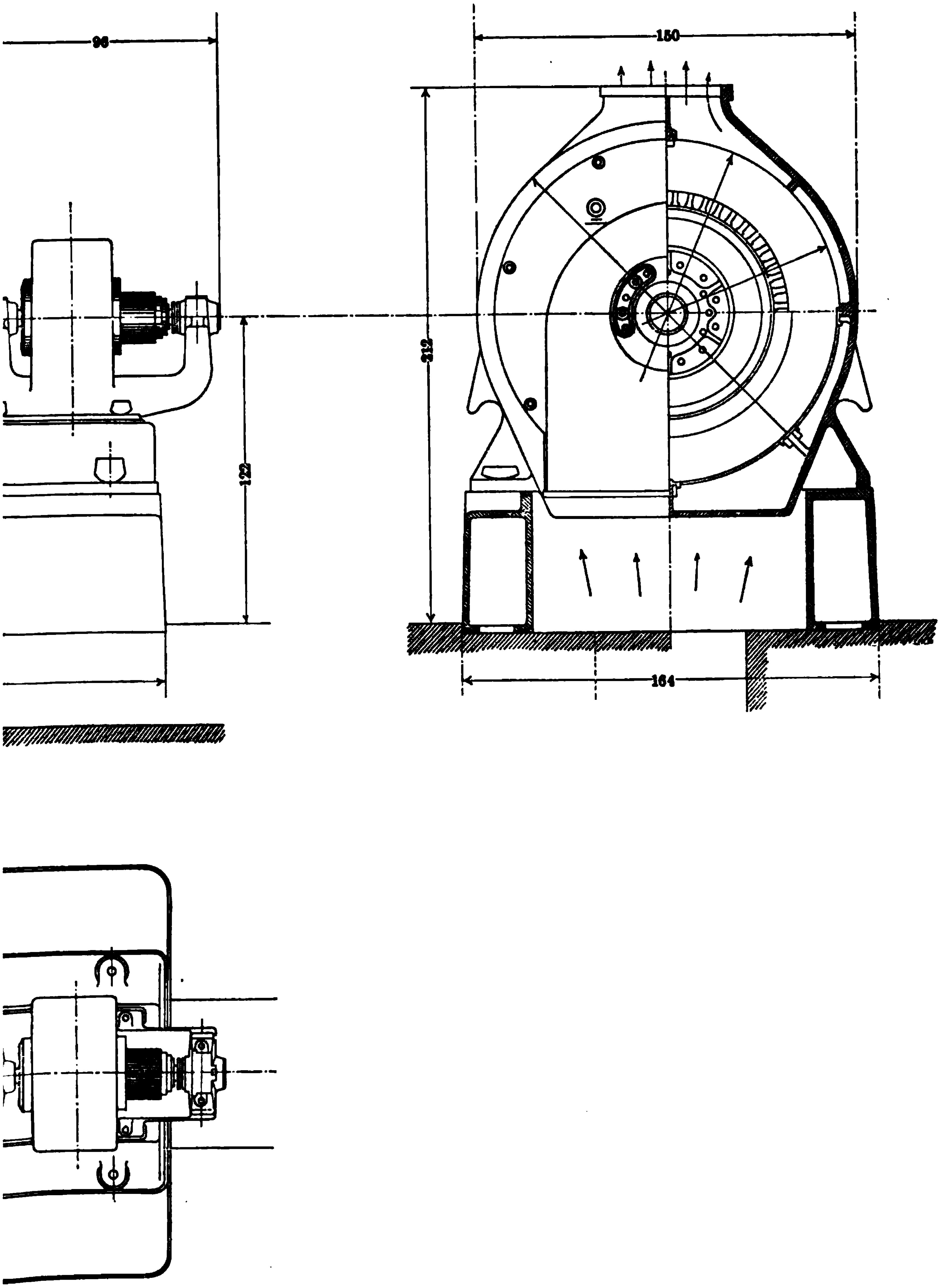


FIG. 217. — General arrangement of 1000 kva. 1500 r.p.m. 200



volt standard 3-phase alternator— Brown Boveri & Co.

lation of the core plates is obtained by japanning the stampings, and noise due to the vibration of the laminations is avoided by

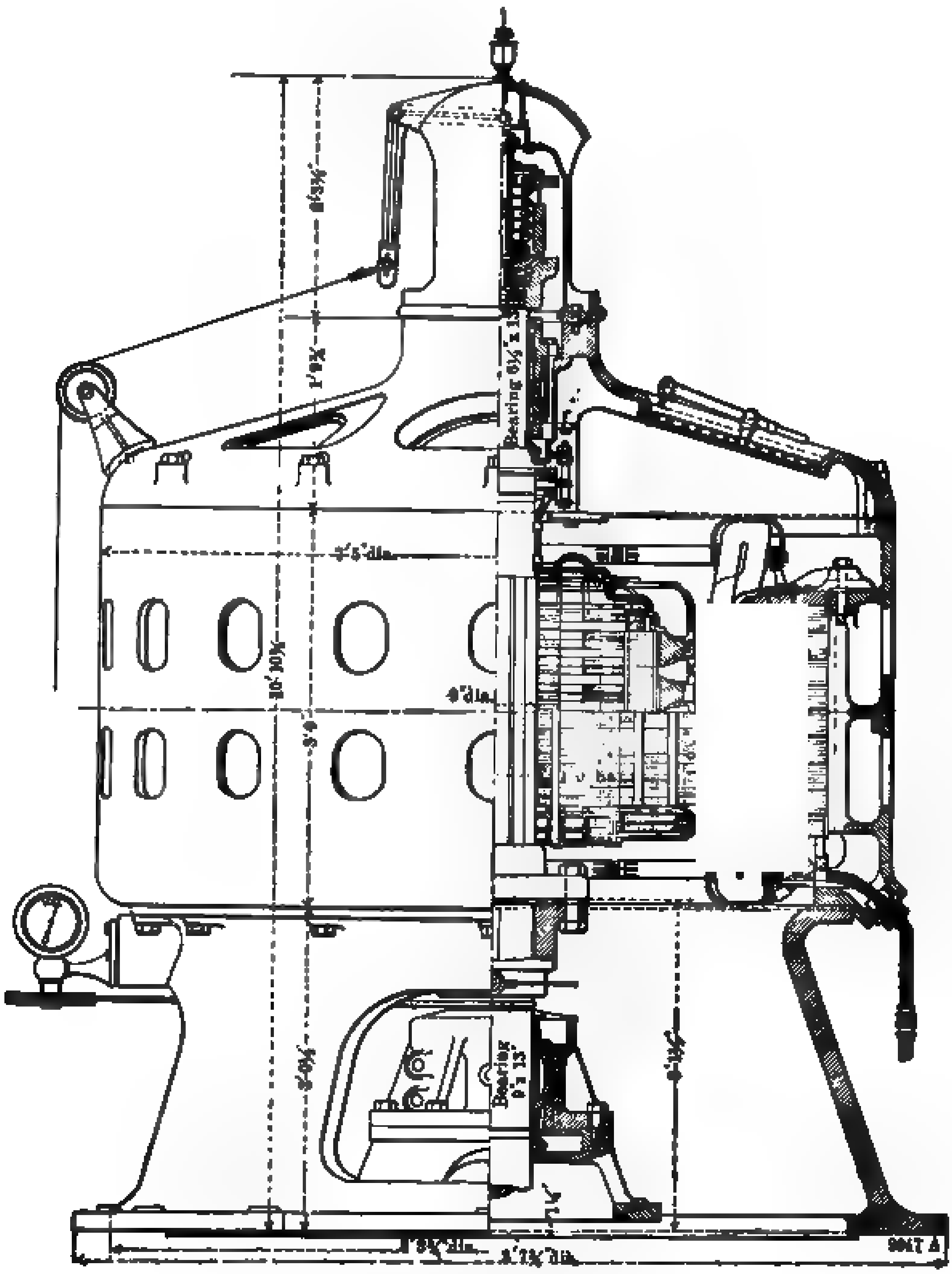


FIG. 218. — General arrangement of 1500 kva. 1000 R.P.M. 6-pole British Thomson-Houston alternator.

using very heavy end flanges with strong fingers projecting between each slot.

Illustrations and details of the armature winding have already been given on pages 238 to 240.

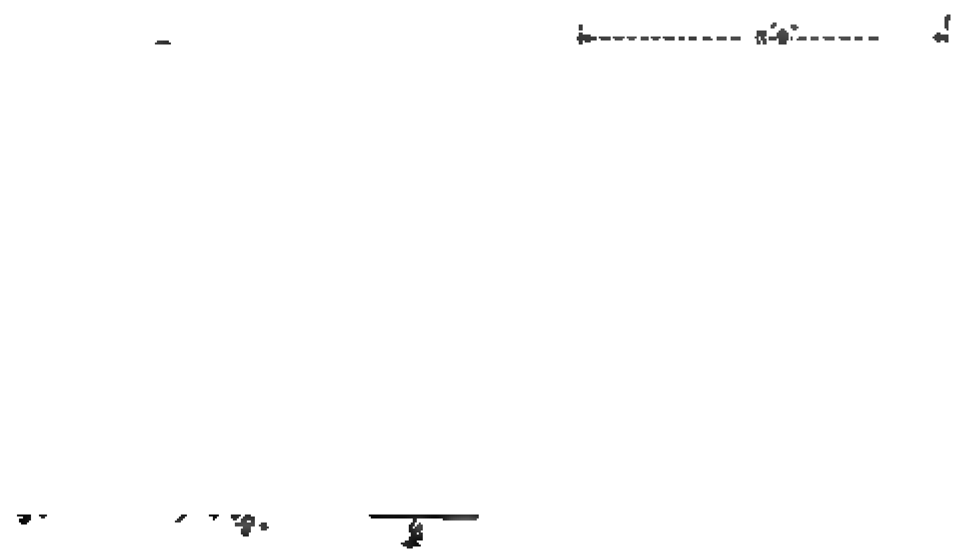


FIG. 219. — Stator frame of British Thomson-Houston Co.'s 1500 kva. turbo-alternator.

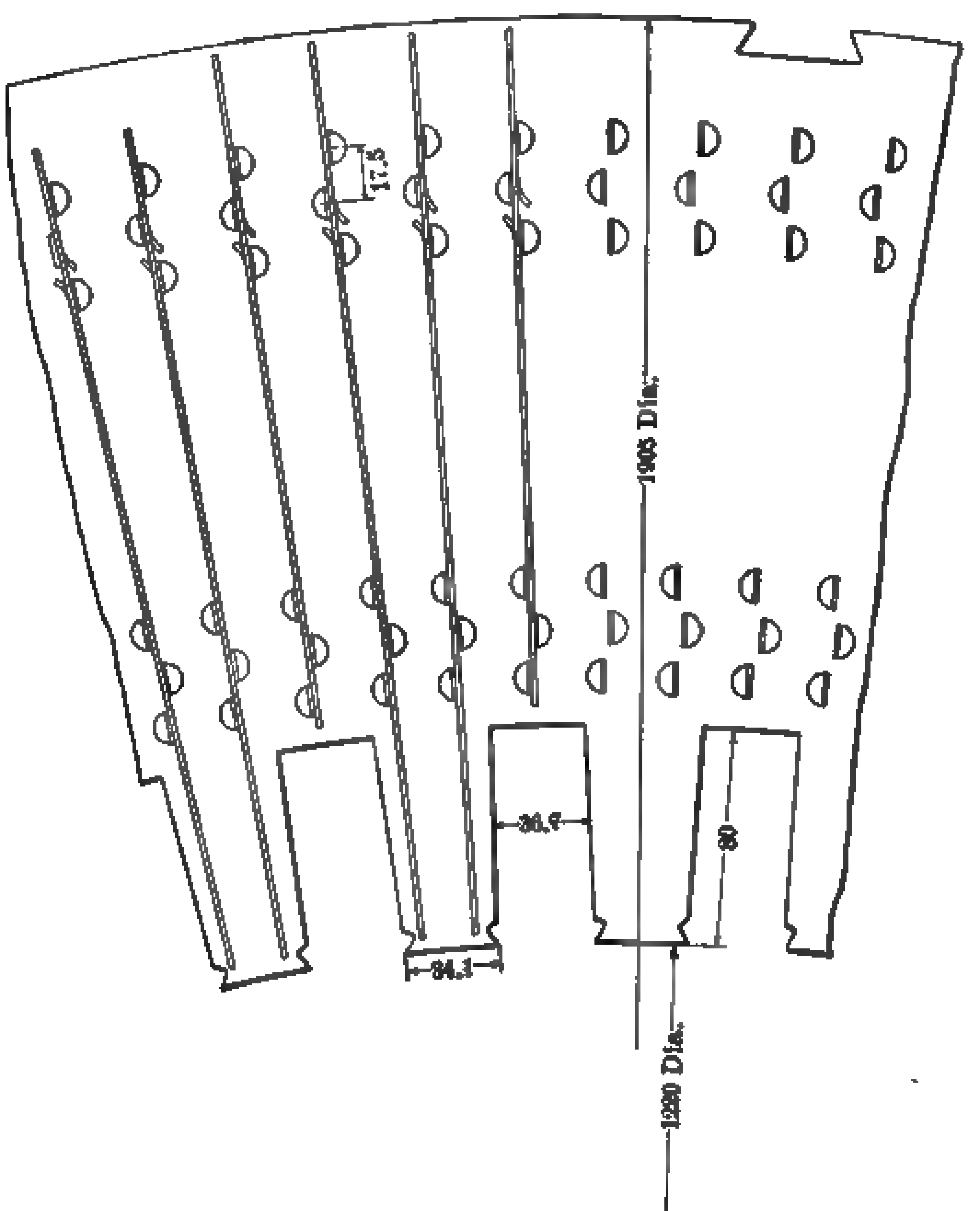


FIG. 219 *A*. — Armature laminations of British Thomson-Houston Co.'s 1500 kva. turbo-alternator.

The following description of the treatment of the coils is given by H. S. Meyer.*

The moisture is first all extracted by a vacuum process and then the interstices of the coil are completely filled with an asphalt compound, which is solid at the running temperatures of the machine. This filling, in addition to preventing the accumulation of moisture inside the coil through the movement of the air caused by the variation in temperature, also serves to conduct the heat out of the coil at a much faster rate than if air were present, and therefore leads to a lower temperature of the winding. The coils thus filled are covered with a number of layers of tape, each treated in such a manner that a glaze of oxidised oil is formed, which is practically infusible, and which acts as a permanent seal to the fibres of the tape. The finished coils are entirely moisture and oil proof, remaining at the same time sufficiently flexible to lend themselves readily to bending and clamping so that vibrations or severe strains, such as are caused by wrong paralleling or short circuits of the system, have hardly any effect.

The ends of the coils are held against the stator casing by special clamps, embracing the coils and bolted on to the armature end plates. These are illustrated in Fig. 218.

The connections from coil to coil are brought round at the back of these clamping blocks, and the terminal cables emerge through bushed holes at the bottom corner of the casing.

The rotor construction is designed to meet the high mechanical stresses set up in the pole pieces and field windings when rotating at high speeds.

The magnet system is of the definite pole construction, having six salient poles, as shown in Figs. 220 and 222. The whole structure is built up of sheet-iron stampings, constituting a hexagonal hub having two axial *T*-grooves in each of its faces, into which fit correspondingly shaped projections on the pole piece stampings, the latter being secured by keys driven in from each end. This arrangement forms a good method of attaching and securing pole pieces.

The field winding consists of two coils on each pole with a ventilating space (1.25 cms or ½ inch) wide between them, thus increasing the cooling facilities and allowing higher current densities to be employed.

* *Electrician*, p. 449, Jan. 12, 1906.

Each coil is composed of copper strip wound on edge. The section of the strip is 25 mm. by 0.9 mm. (1 inch by 0.035 inch) and there is 0.18 mm. (0.007 inch) of insulation between turns.

For convenience in connecting up the coils, they are wound alternately right hand and left hand, the beginning of one coil and the

FIG. 220. — Rotating field construction of 1500 kva. British Thomson-Houston turbo-alternator.

end of the next being thus brought out near one another at the same end of the rotor.

The coils are thoroughly secured against any tendency to shift or fly out. This is accomplished in the following manner:

The coils are clamped between two perforated bobbin flanges, one bedding on the hub and the other on the underside of the pole tip, the overhanging pole tip thus taking up the axial component of the centrifugal force of the sides of the coil.

The lateral component of the centrifugal force of the sides of the coil (i.e., across the pole at right angles to its axis) is taken up by special supporting brackets placed between the poles, and secured on to the hub by bolts whose heads engage in grooves formed in the stampings at the corners of the hexagonal hub.

The end portions of the field coils are secured against radial forces by a wrought-iron strap dropped over the coil, and secured at its lower end by projections fitting into the *T*-grooves below the pole seat. The wedges which hold the pole pieces also serve for these straps. In this way the field windings should be firmly secured

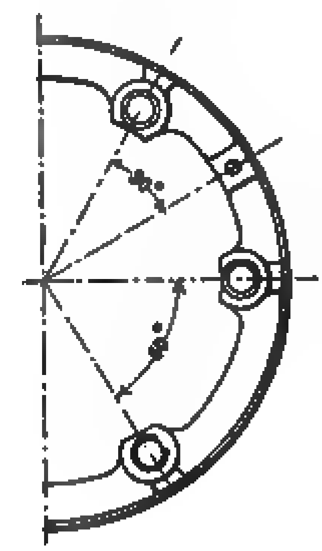


Fig. 220 *A*. — Field collector, rings of 1500 kva. British Thomson-Houston turbo-alternator.

against any tendency to shift or budge. This is most important at these high speeds. The diameter at the pole face is 120 centimetres ($47\frac{1}{4}$ in.) giving a peripheral speed of 63 meters per second (12,500 ft. per minute). The slip rings for the field winding are shown in Fig. 220 *A*.

Ventilation. — The ventilation of turbo-generators is always a difficult problem, and in the case of vertical shaft machines, in addition to the ordinary provisions, it is necessary to avoid the upward draught of hot air or steam which would be introduced by the close proximity of the turbine immediately underneath the generator.

In this machine a downward draught of cool air is induced by cups

at the upper end of the rotor ventilating tunnels. These are clearly shown in the illustration of the rotor, Fig. 222. In addition the angle brackets between the magnet cores are provided with vanes

FIG. 221.— 1500 kva. 1000 r.p.m. British Thomson-Houston Curtis turbo-alternator.

which project radially, after the manner of a fan, and thus compel the air in the gap to take a downward path.

These vanes are also shown in Fig. 222 from which it may be seen that there are four vanes between each pair of poles.

There are seven ventilating ducts in the stator, which provide a ready path for the air to reach the series of holes in the stator frame.

The following is an abbreviated list of the more important dimensions of this machine, which are here tabulated in both Metric and British units, as the latter are the dimensions appearing on the drawings in Figs. 218, 219 and 220. A complete specification will be found on pages 93 to 96 of Chapter VI, where may also be found a study of the design and its constants.

FIG. 222.— Rotating field of 1500 kva. British Thomson-Houston turbo-alternator.

Further information concerning this machine will be found as follows: Calculation of stresses, pages 310 to 318; description of winding, pages 238 to 240.

Data for 3-Phase, 1500 kva. Alternator.

Rated output at unity power factor — kw.	1500
Number of phases	3
Periodicity in cycles per second	50
Speed in revolutions per minute	1000
Number of poles	6
Terminal voltage	11,000
Current per terminal at full load	79.5
Connection of armature	*Y*
Voltage per phase	6350

Data for Armature Iron.

External diameter of laminations	190.5 cms.	75 in.
Internal diameter of laminations at air gap (D)	122 "	48 "
Gross length of core between flanges λg	58.2 "	23 "
Number of ventilating ducts	7	7
Width of each duct	1.27 cms.	0.5 cms.
Effective length of core (iron) λn	44.5 "	17.5 "
Number of slots	54	54
Depth of slot	8 cms.	3.15 in.
Width of slot	3.68 "	1.45 "
Pitch of slots at armature face	7.1 "	2.79 "

Data for Rotating Field.

Number of poles	6	6
Diameter at pole face	120 cms.	47.125 in
Radial depth of air gap, maximum	2.22 "	0.875 "
Radial depth of air gap, minimum	1.11 "	0.4375 "
Radial depth of air gap, mean	1.45 "	0.582 "
Pole pitch at air gap	62.8 "	25.1 "
Circumferential length of pole arc	38 "	16.6 "
Ratio of pole arc to pole pitch	0.66	0.66
Length of pole piece parallel to shaft	55.2 cms.	21.75 in.
Breadth across pole body	25.4 "	10 "
Breadth across pole tips	36.6 "	16 "
Distance between pole tips	18.3 "	8 "
Radial length of pole piece from pole face to seat	21.7 "	9.5 "

Data for Armature Winding.

Number of conductors per slot		18
Number of turns in series per phase		162
Nature of conductor		Pressed cable
Depth of conductor	0.436 cm.	0.172 in.
Width of conductor	0.872 "	0.344 "

Data for Field Winding.

Total number of field coils		12
Number of field coils per pole		2
Turns in series per coil		150
Turns in series per pole		300
Width of strip	2.54 cms.	1.0 in.
Thickness of strip	0.09 "	0.035 "

Weights.

	Metric-Tons.
Magnet cores	1.7
Magnet yoke	1.2
Armature laminations	5.1
Effective iron, total	8.0
Armature copper	0.45
Field copper	0.75
Effective copper, total	1.2
Total weight of effective material	9.2
Stator complete with end shield	11.4
Rotor complete with shaft and half coupling	5
Total weight of alternator	16.4

Data from Test Report.

The no-load saturation curve taken with increasing and decreasing excitation is shown in Fig. 223, the excitation for normal voltage at no load being 11,000 ampere turns per pole, corresponding to 37 amperes in the field circuit. The exciting power for 1375 kilowatts non-inductive load at 11,000 terminal volts was 4.5 kilowatts, the exciter voltage being 220.

The machine was run on quarter-load for 1½ hours, half-load for 2 hours, and full load for 3 hours, successively. After this run the final temperatures observed at various parts of the machine were as follows:

	Degrees Cent.
Rotor spools	35
Pole tips	35
Stator winding (back)	39
Stator winding (front)	36
Core ducts	45
Teeth	44

The temperature of the atmosphere was 21 degrees C., and the temperature rises were therefore:

	Degrees Cent.
Rotor spools	14
Pole tips	14
Stator winding (back)	18
Stator winding (front)	15
Core ducts	24
Teeth	23

The relatively low rises observed after the run are a consequence of the liberal provision for ventilation. The temperature rise in any part of the generator after running on full load at 100 per cent power factor was guaranteed not to exceed 40 degrees C.

The calculated excitation required for full load and unity power factor is 11,600 ampere turns. From the saturation curve, Fig. 223,

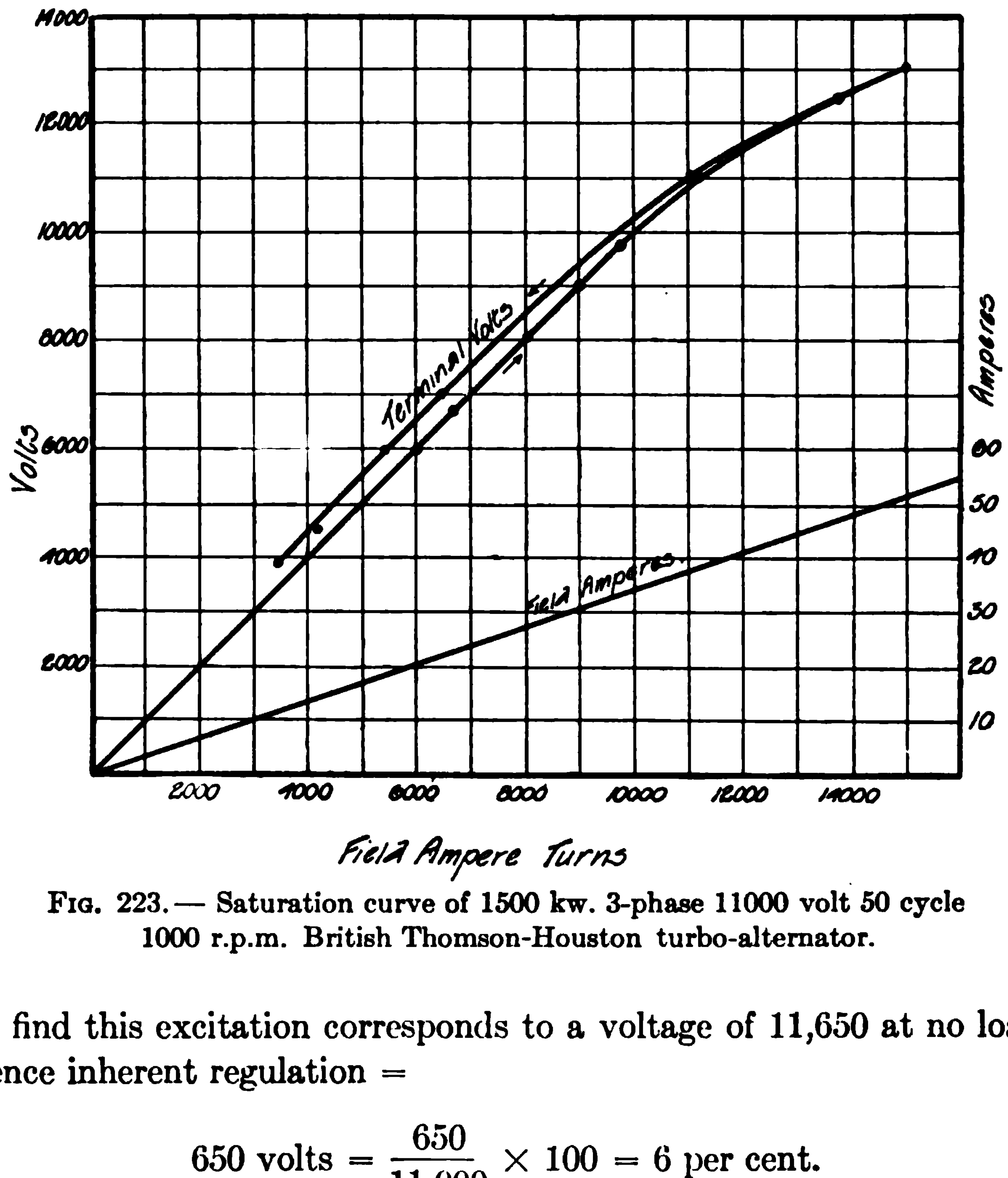


FIG. 223.— Saturation curve of 1500 kw. 3-phase 11000 volt 50 cycle 1000 r.p.m. British Thomson-Houston turbo-alternator.

we find this excitation corresponds to a voltage of 11,650 at no load. Hence inherent regulation =

$$650 \text{ volts} = \frac{650}{11,000} \times 100 = 6 \text{ per cent.}$$

At a power factor of 0.8, the excitation for full load becomes 15,000 ampere turns. From the saturation curve, Fig. 223, we see that this excitation at no load would give a voltage of 13,000. Hence

$$\text{Inherent Regulation} = \frac{2000}{11,000} \times 100 = 18 \text{ per cent.}$$

Description of a 500-Kw. 3-Phase 4-Pole 1500 R.P.M. 50-Cycle 550-Volt Turbo-Alternator by Messrs. Scott & Mountain.

This machine, which is of the horizontal type, is intended for direct coupling to a Parsons steam turbine. Fig. 224 is a general arrangement of the machine and shows some of the principal details of the construction. The armature laminations are housed in a cast iron frame of the enclosed type and arranged for forced draught ventilation. One of the end flanges of the armature core is cast in one piece with the frame, the other end flange being a plain cast iron ring keyed to the frame. There are nine ventilating ducts in the armature core, each 1.25 cms. ($\frac{1}{2}$ inch) wide.

Fig. 225 shows the armature winding diagram and the connection of the machine, and also the details of the slot.

The winding may be described as a spiral wave winding. The revolving field is of the laminated smooth core slotted type, the field winding being distributed in six slots per pole.

Fig. 224.— General arrangements of 500 kw. 3-phase 4-pole 1500 r.p.m. 50 cycle 550 volt Scott & Mountain turbo-alternator.

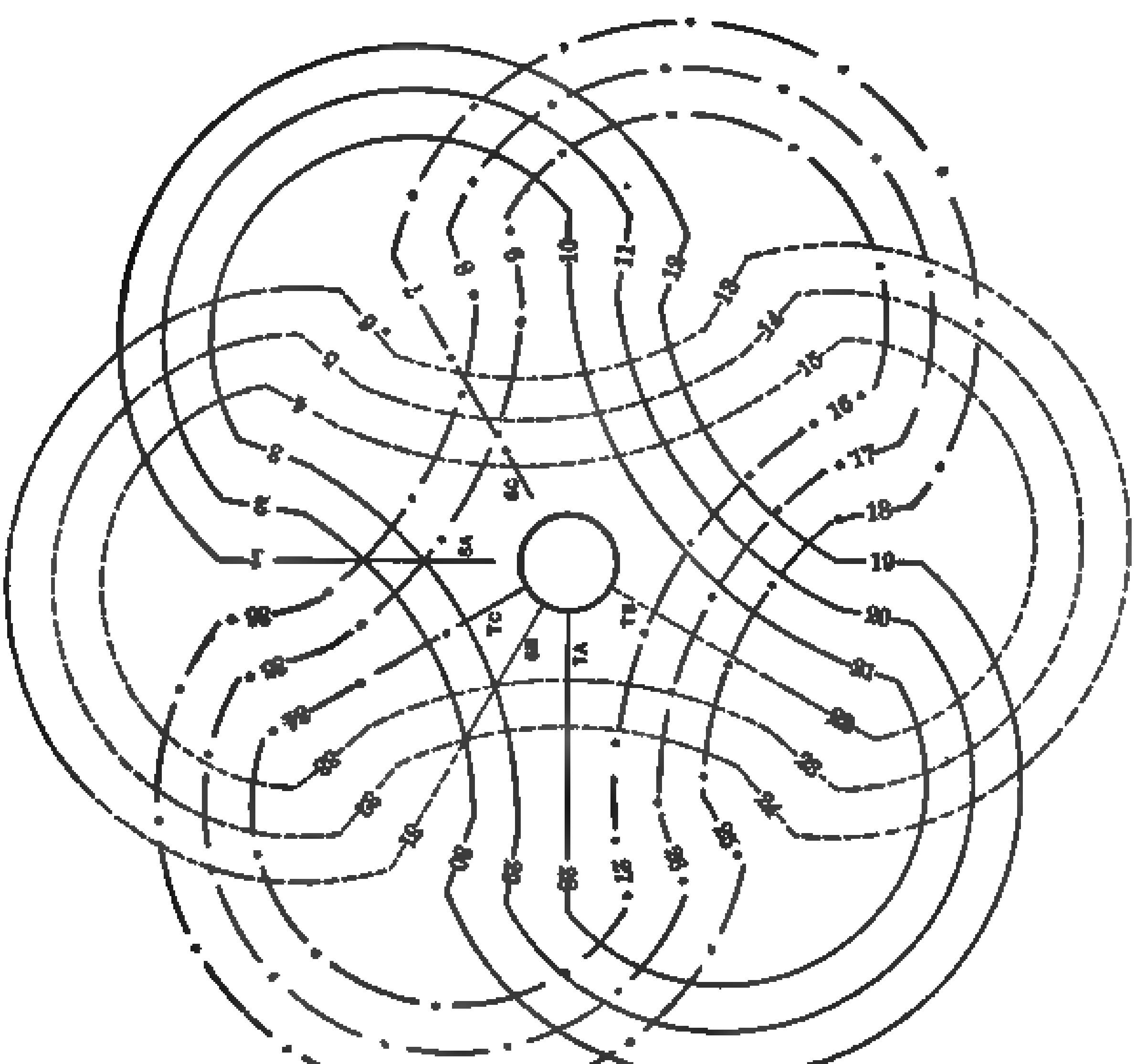


FIG. 225. — Armature winding diagram and details of slot of 500 kva. 3-phase turbo-alternator.

The core is built up from laminations of No. 28 gauge thickness, mounted direct on the shaft. Nine ventilating ducts are provided, immediately opposite to those in the armature core. The details of the stampings and of the spacing pieces for the ventilating ducts are shown in Fig. 226.

Efficient ventilation is obtained from the four triangular tunnels in the stampings through which the air is impelled by the vanes situated on the rotor end flanges. The air, thus drawn through the core,

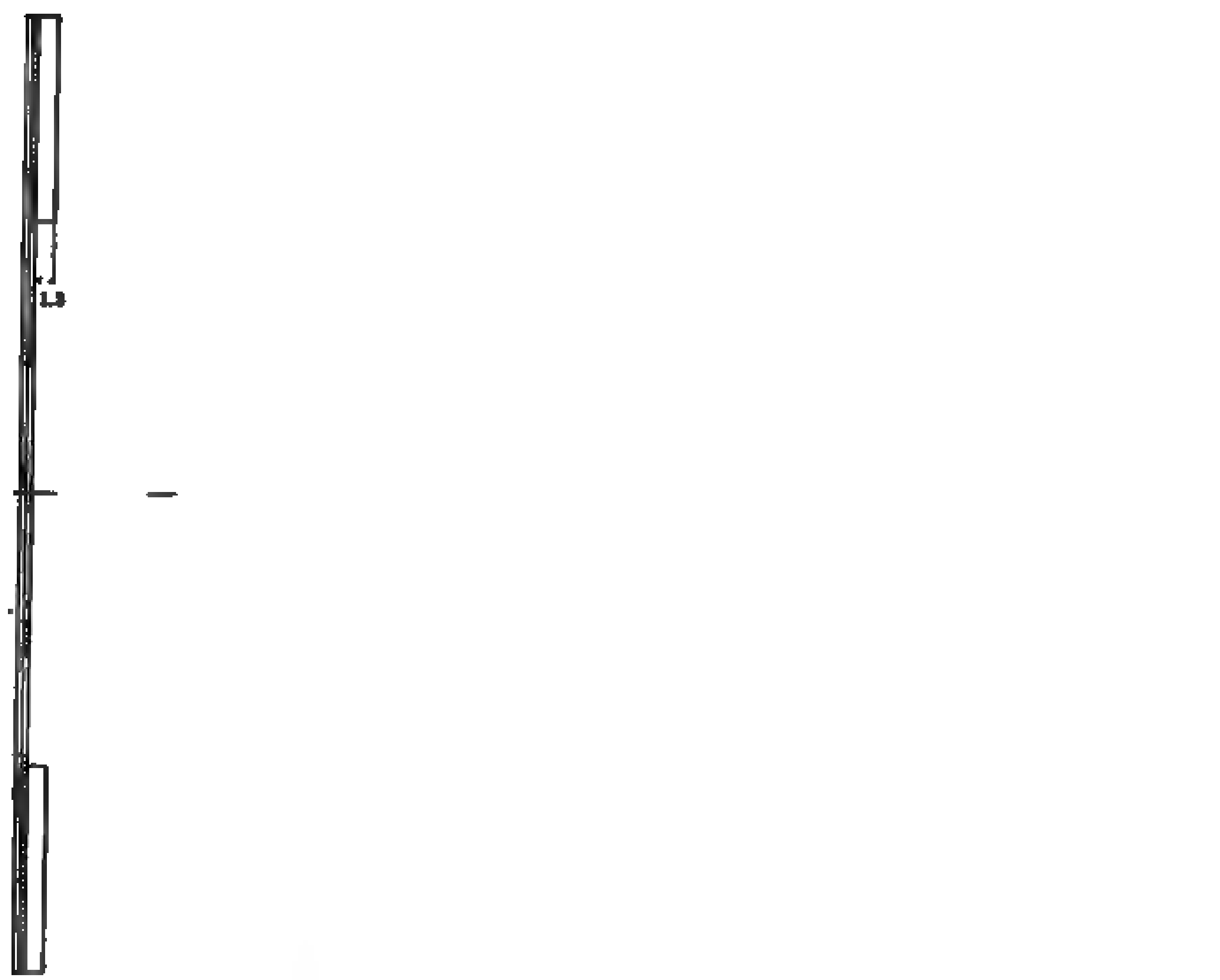


FIG. 226.— Rotor lamination of 500 kva. Scott and Mountain turbo-alternator.

finds egress through the ventilating ducts of the rotor and the stator, and finally passes out through the large hole in the top of the main stator casing. The latter has no openings except one at the base and one at the top, the air entering at the bottom and leaving at the top.

The mid polar parts of the stampings are left unslotted, chiefly to avoid the heavy stress which would be incurred by punching slots

all round the periphery of the rotor. The stress on the embedded portion of the field winding is resisted by substantial wedges driven into the dovetails at the mouth of the slot. The end connections are completely enclosed by the cylindrical end covers which are bolted on the end flanges of the rotor core. Facing pieces are provided both on the end flanges and on the end covers to provide a path for the air to circulate round the end windings.

The following gives the specification and technical data for this machine:

SPECIFICATION FOR

500-Kw. 3-Phase 4-Pole 1500 R.P.M.

50-Cycle 550-Volt

ALTERNATING CURRENT GENERATOR.

Dimensions in Centimetres.

Output in kilowatts		500
Output in kva. (power factor 0.85)		590
Terminal voltage		550
Style of connection		Δ
Current per terminal		615
Speed R.P.M.		1500
Frequency		50
Number of poles		4
Armature Iron		
Diameter at air gap	D	68.6
Diameter at bottom of slots		76.9
External diameter of laminations		114.4
Gross length between coreheads	λg	68.6
Number of ventilating ducts		9
Width of each duct		1.27
Effective core length (iron)	λn	51.3
Width of the end ducts		1.27
Slots and Teeth		
Total number of slots		36
Slot pitch at armature face		6.02
Width of slot		2.54
Width of tooth at armature face		3.46
Radial depth of slot		4.13

SPECIFICATION FOR ALTERNATING CURRENT GENERATOR. — *Continued.*

Armature Copper

Number of slots per pole per phase	3
Number of conductors per slot	2
Section of conductor	4×0.543
Current in amperes	615
Current density — amperes per square centimetre	293
Dimensions of conductor bare	3.05×0.178
Thickness of slot insulation	0.254
Number of turns in series per phase	12

Rotor Iron

Diameter at pole face	65.4
Length of air gap (minimum)	1.59
Pole pitch at air gap	51.35
Circumferential length of pole arc	34.1
Ratio pole arc/pole pitch	0.64
Gross axial length (parallel to shaft)	68.6
Effective axial length of pole (iron)	51.3
Total number of slots	24
Slots per pole	6
Depth of slot	10.16
Width of slot	2.54

Field Copper

Number of conductors per slot	39
Number of turns per pole	117
Section of conductor	0.436
Current in amperes	120
Current density	280
Dimensions of conductor	0.686×0.636
Total area of copper in slot	17
Total area of slot	25.8
Space factor	0.66
Mean length of turn	230
Resistance of all coils at 60° C.	0.494
Volts across field at 120 amperes	60
Exciter voltage	100

Magnetic Data

Armature flux per pole (550 volts)	12.3
Leakage coefficient	1.25
Flux in the pole core	15.4

SPECIFICATION FOR ALTERNATING CURRENT GENERATOR. — *Continued*

Magnetic Densities (lines per square centimetre)	
Armature	5,240
Teeth	13,000
Pole core — laminations	18,100
Yoke	19,650
At pole face	7,120
Losses	
Armature Copper	
Mean length of turn	347
Resistance per phase at 60° C.	0.00383
Total I^2R loss at $\cos\phi = 0.85$	4.35
Armature Iron	
Weight of armature iron (excluding teeth) — tons	2.0
Frequency	54
Flux density — kilolines per square centimetre	5.24
Kilowatts per ton	3.0
Total core loss — kilowatts	7.3
Teeth	
Weight of teeth — tons	0.226
Flux density — kilolines per square centimetre	13
Kilowatts per ton	18.6
Total tooth loss — kilowatts	4.2
Total iron loss — kilowatts	11.5
Watts per sq. in. of armature surface $\pi D \times (\lambda g + 0.7\tau)$ $\cos\phi = 0.85$	70
Field Copper	
Power for excitation at full load $\cos\phi = 0.85$	7.2
Watts per sq. in. of external surface of field spool $\cos\phi = 0.85$	40
Efficiency. At full load and power factor $= 0.85$	
Armature copper loss	4.35
Armature iron loss	11.5
Field copper loss	7.2
Total calculable losses	23.05
Efficiency — per cent (excluding friction, etc.)	95.5
Constants and Coefficients	
Weight of effective material — kilograms	5540
Weight of effective material — kilograms per kilowatt	9.4
Cost of effective material (dollars)	1020
Cost of effective material per kilowatt (dollars)	1.74
$D^2\lambda g$ (decimetres)	323

SPECIFICATION FOR ALTERNATING CURRENT GENERATOR — *Continued.*

LOSSES — *Constants and Coefficients* — continued	
Output coefficient (ξ)	1.21
Ratio $\frac{\lambda g}{\tau}$	1.34
Ratio $\frac{D}{\lambda g}$	1
Ampere conductors per centimetre of periphery (α)	206
Flux per square centimetre of armature surface (β)	3336
Peripheral speed — metres per second	53.8
Watts per cubic centimetre of active belt	9.1
Ratio of field ampere turns to armature ampere turns	2.7
Kva. per pole	147

Weight of Material.

	Metric Tons.
Field laminations	2.25
Armature laminations	2.63
Total effective iron	4.88
Armature copper	0.24
Field copper	0.42
Total copper	0.66
Total effective material	5.5

Cost of Material.

	Dollars.
Field laminations	280
Armature laminations	330
Total effective iron	610
Armature copper	150
Field copper	260
Total copper	410
Total effective material	1020

Heyland Compounded Turbo-Alternator.

Fig. 227 illustrates a design by Heyland for a two-pole turbo-alternator. This design is a modification of that proposed by Heyland for slow speed revolving field alternators,* in which a compounding effect was obtained by arranging the exciter field in shunt with the field of the alternator. For a high speed turbo-alternator, however, a revolving armature gives a more satisfactory design, and

* *Electrician,* Vol. LVIII, p. 42.

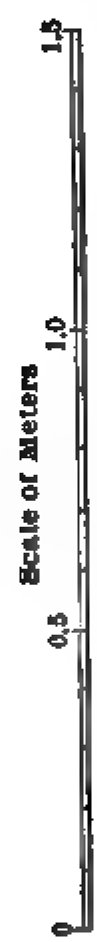


Fig. 227.— Heyland's 600-kw. 2-pole 3000 R.P.M. 330-volt 50-cycle 3-phase self-compounding alternator.

the design shown in the figure is a modification to suit a revolving armature construction.

By the application of the principle of the magnetic shunt, the machine is made self-regulating and a large saving in material is effected, as the heating and mechanical properties are the only factors to be considered when proportioning the machine. The principle of the machine is best explained by means of the diagram in Fig. 227 *A*.

In this figure *N*, *S*, are the two main field poles of the alternator, and *n*, *s*, are the two field poles of the exciter arranged as a shunt to the main field circuit. The two north poles and the two south poles respectively, are surrounded by common exciting coils. At no load the fluxes in the two field circuits will be inversely proportional to the respective reluctances (the demagnetising effect of the exciter armature ampere turns

will be negligible), and these reluctances must be so proportioned that correct excitation is obtained. As the alternator is loaded, the armature ampere turns tend to decrease the flux in the main field circuit, but to *increase* the flux in the exciter circuit to a degree dependent on the value of the armature *demagnetising* ampere turns.

Thus the machine is compounded for both load and power factor. By suitable adjustment of the relative reluctances of the two field

FIG. 227*A*. — Diagrammatic sketch of Heyland's self-compounding alternator.

circuits and, if necessary, by the addition of auxiliary field winding on the main poles in series or shunt with the main field winding any desired amount of compounding can be obtained, and if necessary the machine can be overcompounded.

With the above arrangement the regulation of exciter field (and exciter voltage) is absolutely in step with the fluctuations in the load. The design illustrated is for a 500-kw. 3000 R.P.M. 50-cycle machine.

The following data for a rotating armature compounding machine and for a rotating field design for the same output, etc., are taken from Heyland's article.*

* *Electrician*, Vol. LVIII, p. 998.

TABLE 41.

COMPARISON OF HEYLAND SELF-COMPOUNDING ALTERNATOR WITH ORDINARY ROTATING FIELD ALTERNATOR.

	Heyland Rotating Armature Design.	Ordinary Rotating Field Design.
Output in kilowatts	500	500
Speed in revolutions per minute	3,000	3,000
Frequency	50	50
Voltage drop non-inductive load	$\cong 0$	10%
Voltage drop purely inductive load	$\cong 0$	30%
Armature diameter D	60 cm.	54 cm.
Gross core length λg	50 cm.	70 cm.
Air gap	0.5 cm.	2.0 cm.
External diameter of frame	90 cm.	100 cm.
Air gap of exciter	1.5 cm.	...
Weight of active material in the alternator and exciter (approx.)		
(1) Steel magnet frame	1,400 kg.	600 kg.
(2) Armature stampings	600 kg.	2,100 kg.
(3) Field copper	300 kg.	600 kg.
(4) Armature copper	150 kg.	150 kg.
Total	2,350 kg.	3,450 kg.
Losses in alternator and exciter		
(1) Iron losses	5,000 watts	15,000 watts
(2) Copper losses in field winding at non-inductive full load	2,000 watts	6,000 watts
(3) Copper losses in armature at non-inductive full load	3,000 watts	3,000 watts
Total	10,000 watts	24,000 watts

CHAPTER XII.

STRESSES IN ROTATING FIELD SYSTEMS.

In this chapter we shall deal briefly with the principal stresses occurring in rotating field systems in so far as these stresses are associated with the electromagnetic design.

The necessary calculations are made for determining whether the proportions of the electrical and magnetic parts are consistent with the required mechanical conditions, and whether the stresses due to the windings are within safe limits. As noted in Chapter VII, while these calculations need not go so far as to determine in detail the dimensions of the mechanical accessories on the rotor, they must nevertheless be carried far enough to show the general order of the principal stresses and the value of the safety factors. The matter may best be dealt with by considering one or two examples, and outlining the necessary calculations.

As a first example let us take the 650-kw. 4-pole alternator designed in Chapter VII, and let us carry out the stress calculations for an alternative construction of rotor to that given in Chapter VII. In the present chapter we have taken a laminated pole structure with the poles dovetailed into a solid steel hub, while in Chapter VII the rotor structure is shown as consisting of four slabs of solid steel each comprising four magnet cores and the hub in one piece.

For the average values of working stress in the various materials, and for the ultimate stresses and safety factors, reference may be made to Table 11 on pages 63 and 64 of Chapter IV dealing with materials.

Stresses on the Pole Cores. — The magnet core is subjected to a radial stress due to its own centrifugal force, and to that of the field windings. It is also due to the magnetic pull of the armature. The magnetic pull on the pole is

$$F_1 = 0.00004\, B^2 A,$$

where F_1 = magnetic pull in tons,
B = pole face density in kilolines per square centimetre,
A = area of pole face in square centimetres.

For the design in question we have

B = 6.1 kilolines per square centimetre,
A = 2220 square centimetres,

whence $F_1 = 3.3$ tons.

The centrifugal force of the magnet core is

$$F_2 = 0.0000112\, MRN^2,$$

where

F_2 = centrifugal force of the magnet core in tons,
M = mass of magnet core in tons,
R = mean radius in centimetres,
N = speed in R.P.M.

The weight of one magnet core and its field coil is 0.215 ton, and its mean radius is 32 centimetres. N, the speed in revolutions per minute, is equal to 1500.

$$\therefore F_2 = 174 \text{ tons.*}$$

The total radial pull on the pole is therefore $F_1 + F_2 = 177$ tons. This force has to be resisted in tension at the root of the magnet core at the section marked ab (Fig. 228). The length of ab = 7.5 centimetres, and the net (iron) length of the magnet core, parallel to shaft, = 42 centimetres.

The cross section of iron at ab is therefore

$$7.5 \times 42 = 315 \text{ square centimetres.}$$

Hence the stress at the root of the magnet core is

$$\frac{177}{315} = 0.56 \text{ ton per square centimetre.}$$

This is an average working value for wrought iron in tension, and corresponds to a factor of safety of 7 when referred to the ultimate strength. (See Table 11, pages 63 and 64.)

There is also a shearing stress on the dovetail pieces. This is resisted by two sections, as indicated by the lines ac and bd (Fig. 228). The length of the dovetail at ac = 3.5 centimetres, and the axial

* It would be more strictly correct to add to the centrifugal force of the iron of the magnet core, the axial components, mp (Fig. 228), of the centrifugal force of the field coil, plus the centrifugal force of the coil ends, but taking the whole centrifugal force of the copper insures erring on the safe side.

length of the magnet core = 42 centimetres. Hence the aggregate area of the two sections *ac* and *bd* = 42 × 3.5 × 2 = 294 square centimetres. The shearing stress across the corners of the dovetail is thus:

$$\frac{177}{294} = 0.6 \text{ ton per square centimetre.}$$

In cases where this stress is found to be excessive, it can be reduced by splitting the single dovetail into two, each of which can be of smaller depth. The number of sections in shear (as *ac*) is thus

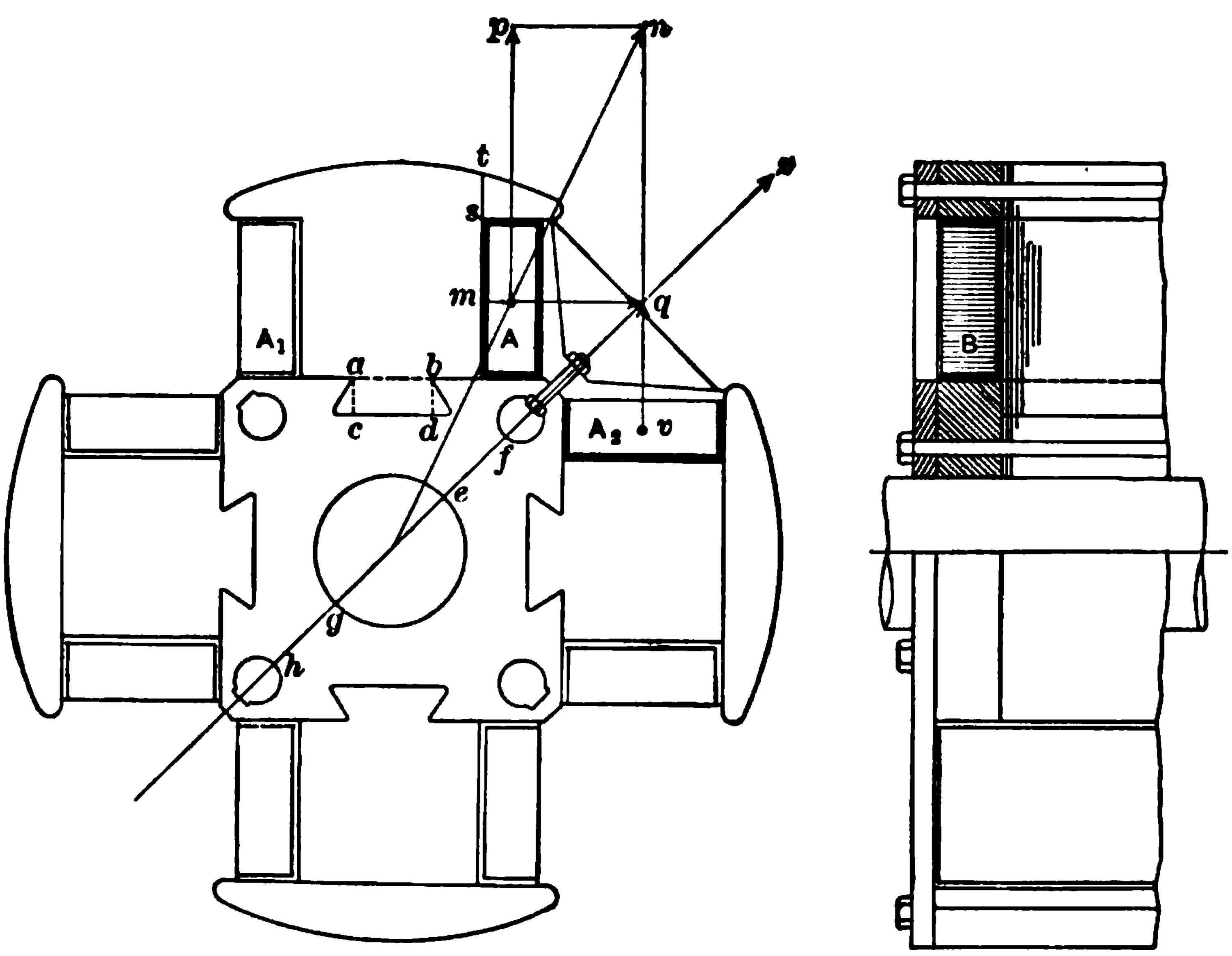


FIG. 228.— Rotating field for 4-pole 50-kva. alternator.

increased to four. The total cross section resisting the force may by such means be increased to any desired amount. In cases similar to those shown in Figs. 204 *a*, *f* and *l*, where the poles are secured by a bar or bars extending the whole length of the machine, the shear stress on the bars must be calculated, using the total area presented by all the sections of the bar in shear, and the bars must be of sufficient size to resist the stress. The shear stress across projections on the pole or hub, similar to *ac* (Fig. 228), should also be calculated, and also the tensile stress at the root of the magnet core at its narrowest part, which in the case considered is at *ab* (see previous page).

Stress in the Hub. — There is a tension stress in the hub due to the centrifugal force of the magnet cores and of the hub itself. This stress is similar to the tension stress in the rim of a rotating flywheel. It tends to pull the hub in two, and its magnitude should be calculated for the minimum cross section of the hub, which, in this case, occurs at the lines *ef* and *gh* (Fig. 228). The tension due to the weight of the hub itself is calculated as follows:

The weight of half the hub is 0.19 ton, and its mean radius is 13 centimetres.

Its total centrifugal force is

$$0.0000112 \times (1500)^2 \times 13 \times 0.19 = 62 \text{ tons.}$$

The resultant force normal to sections *ef*, *gh* is

$$\frac{2}{\pi} \times 62 = 39 \text{ tons.}$$

The stress on the sections *ef* and *gh*, due to the centrifugal force of the magnet cores, is obtained from the components of this force normal to the sections *ef* and *gh*.

In the case of a 4-pole structure, the resultant force of two poles on the sections *ef* and *gh* is equal to the $\sqrt{2}$ times the centrifugal force of one pole. In this case the centrifugal force per pole plus the magnetic pull is found to be equal to 177 tons. (See p. 306.)

The resultant of the forces due to the two poles is

$$\sqrt{2} \times 177 = 250 \text{ tons.}$$

The total force on sections *ef*, *gh* is thus:

$$250 + 39 = 289 \text{ tons.}$$

The sectional area at *ef*, *gh* is

$$2 \times 7.5 \times 42 = 63 \text{ square centimetres } (ef = gh = 7.5 \text{ centimetres}).$$

Hence the stress is

$$\frac{289}{63} = 0.46 \text{ ton per square centimetre.}$$

This figure will be slightly exceeded, since the forces due to the field coil supporting brackets have not been taken into account.

Stresses on the Windings. — The field coils are best dealt with by considering each side of a coil separately. Thus, we may first con-

sider the sides of the coil between adjacent poles, and the end parts of the coils marked A and B in Fig. 228. The centrifugal force of the mass of copper A acts in a radial direction, mn. This has two components, (1) mp, the axial component, parallel to the axis of the pole, and (2) mq, the lateral component, normal to the pole axis. The axial component, mp, causes a shearing force and bending moment at the section st on the pole tip, which section must be sufficiently large to resist the stress. The lateral component, mq, tends to make the coil bulge and come away from the pole. It is resisted by angle brackets fixed to the hub as shown. Several methods of fixing these brackets have already been shown in Figs. 197 to 202. The lateral forces on the parts A and A_2 would, if unresisted, set up a tension in the ends of the coils at B, tending to pull the coil in halves. The components mq and vq of adjacent coils A and A_2 give rise to a bending moment on the bracket, which is met by cross ribs of sufficient strength. These components combined, give a radial resultant, qz, and this is the total tension on the retaining bolts or pins with which the brackets are fixed. This stress determines the number and size of these bolts.

It will be seen that the lateral component is smaller the narrower the pole body and the greater the number of poles. Thus with 8-pole and 6-pole machines the stresses in the angle brackets are less than in 4-pole machines. As these brackets constitute an extra amount of rotating inactive material exerting additional stresses on the rotor, an arrangement similar to that shown in Fig. 77 has some advantages. Here the lateral stress is taken up by bars passing between the top and bottom section of the field coil, through ducts between the adjacent slabs of steel of which the poles are built. These bars are secured by keys and plates on the outside of the field coil. The end portions of the field coil, B (Fig. 228), only exert a radial centrifugal force, which may be taken up by an extension of the pole shoe, or by a piece bolted to an end plate, or by means of a metal strap embracing the coil and retained on the hub at its lower ends. The latter construction has been employed by the General Electric Company of U.S.A. and by the British Thomson-Houston Company (Figs. 197 and 198). The strap must be of sufficient section to withstand the centrifugal force of the end portions of the coil B. In the case we have been considering, the weight of field copper on one pole is 0.1 ton. Adding 0.01 ton for weight of

bobbins, etc., this becomes 0.11 ton. The weight of the side of the coil between the poles, (A), is about 0.045 ton, and that of the end portion (B) 0.01 ton. For A, 0.045 ton rotating at 1500 R.P.M. at a mean radius of 28 centimetres, the centrifugal force is 32 tons. The angle which the direction of this force makes with the pole axis is 25 degrees. Hence the axial component

$$mp = 32 \cos 25° = 32 \times 0.9 = 29 \text{ tons},$$

and the lateral component

$$mq = 32 \sin 25° = 14 \text{ tons}.$$

The area of section of the pole tip at st is

$$5.7 \times 42 = 240 \text{ square centimetres},$$

and hence the shearing stress is

$$\frac{29}{240} = 0.12 \text{ ton per square centimetre}.$$

The tension on the bracket bolts, $qz = 14 \times \sqrt{2} = 20$ tons.

This total tension has to be resisted by the bolts at the corners of the hub, which must be of sufficient total cross section to withstand it without exceeding normal stresses.

Allowing a working stress of 1.4 tons per square centimetre, the total section of all the bolts for one bracket is 14 square centimetres.

The number of bolts and the cross section of each should be taken so that the total section amounts to 14 square centimetres.

As a further example let us take the 1500-kva. 6-pole generator fully described and illustrated on pages 284 to 294. The stress calculations for this machine will be similar to those for the previous 4-pole machine, but the arrangements employed for retaining the field windings are different.

It is interesting to compare the relative proportions of the various stresses in the 4-pole and 6-pole cases.

1. Considering first the magnetic pull (F_1) on each pole, we have the pole face density (B) = 8 kilolines per square centimetre. The length of the pole arc = 42 centimetres, and the breadth of the pole (measured parallel to the shaft) = 55 centimetres. This gives us the

area at the póle tips as $42 \times 55 = 2300$ square centimetres, and applying the formula,

$$F_1 = 0.00004\, B^2A,$$

we have the magnetic pull on each pole

$$F_1 = 0.00004 \times 8^2 \times 2300 = 5.9 \text{ tons}.$$

2. Centrifugal force on pole body (F_2).

Winding = 300 turns of copper strip 2.5×0.09 centimetres. Mean length per turn = 180 centimetres.

∴ Weight of copper =

$$8.9 \times 10^{-6} \times 2.5 \times 0.09 \times 180 \times 300 = 0.108 \text{ ton}.$$

If we allow 0.012 ton for flanges and insulation, the spool weight is increased to 0.12 ton.

The weight of iron core = 0.26 ton.

∴ Total weight of pole (M) = 0.38 ton.

Mean radius (R) may be taken as 48 centimetres, and the speed of the machine is 1000 R.P.M.

$$\begin{aligned} \text{We have } F_2 &= 0.0000112\, MRN^2 \\ &= 0.0000112 \times 0.38 \times 48 \times (1000)^2 = 204 \text{ tons}. \end{aligned}$$

$$\begin{aligned} \text{3. Total radial pull} &= F_1 + F_2 \\ &= 5.9 + 204 \\ &= 210 \text{ tons}. \end{aligned}$$

4. This pull is resisted by the laminations at the root of the pole (see Fig. 229), where the effective section ab is equal to

$$2.4 \times 55 \times 2 = 265 \text{ square centimetres}.$$

∴ Working stress at normal speed is equal to

$$\frac{210}{265} = 0.8 \text{ ton per square centimetre}.$$

5. To obtain the shear stress set up in the root of the pole, we have again a strain of 210 tons; and the sections ac and bd across which this strain acts, is also equal to

$$1.2 \times 55 \times 2 \times 2 = 265 \text{ square centimetres}.$$

∴ Shear stress = 0.8 ton per square centimetre.

6. Stress on the hub.

The weight of the laminations forming the hub of the machine, amounts to 1.1 tons. Taking half this weight, we have 0.55 ton, and the mean radius may be taken as 17.5 centimetres.

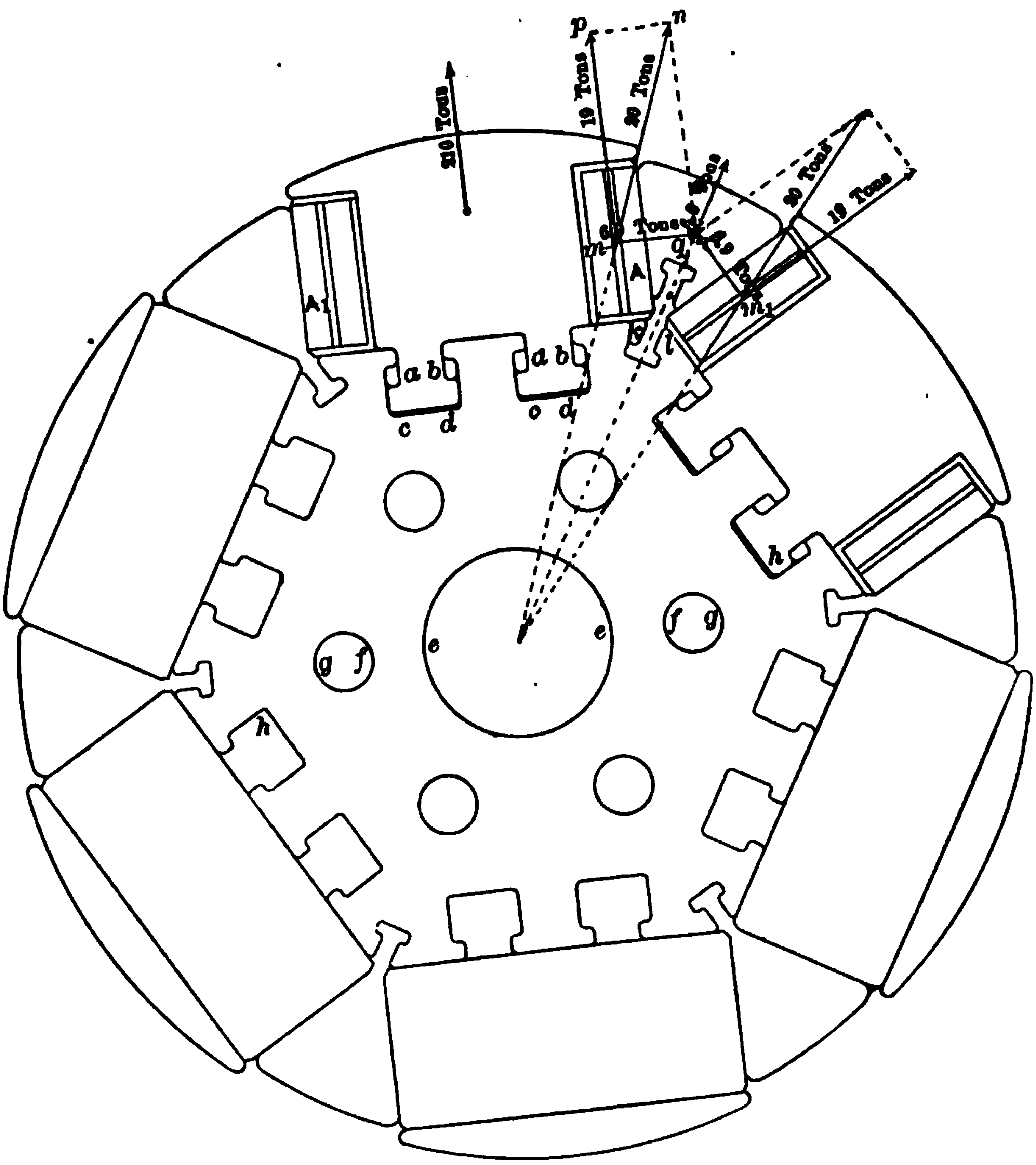


FIG. 229.— Rotating field for 6-pole 1000-kva. alternator.

Therefore the centrifugal force due to the weight of the hub would amount to

$$0.0000112 \times 0.55 \times 17.5 \times (1000)^2 = 108 \text{ tons}$$

if concentrated at one point. But as it is distributed over a semicircle we must multiply this amount by $\frac{2}{\pi}$. We thus obtain the value of 69 tons.

The force along each pole has already been estimated to be 210 tons.

$$\begin{aligned} \therefore \text{ Total force on hub} &= 69 + 210 + 2\ (210 \cos 60^\circ) \\ &= 69 + 210 + 210 \\ &= 489 \text{ tons} \end{aligned}$$

and the least section of the hub (taken across the sections *ef*, *gh*), resisting rupture is equal to

$$2\ (6.4 + 7.5) \times 55 = 2 \times 13.9 \times 55 = 1530 \text{ square centimetres.}$$

$$\therefore \text{ Stress} = \frac{489}{1530} = 0.33 \text{ ton per square centimetre.}$$

7. Let us now calculate in some detail, the forces acting on the coils. The total weight of the coil and bobbin on each pole is 0.108 ton. Let us consider this as made up of two parts, one part consisting of strips parallel to the shaft, and the other part consisting of the ends connecting these strips. The iron core measures 55 centimetres along the shaft, and 25 centimetres along the periphery, so let us assume that two thirds of the centrifugal force due to the weight of the coils is resisted by the pole tips, and that the remaining one third, or 0.036 ton, belonging to the ends, will be provided for by a phosphor bronze strap.

The centrifugal force due to these ends amounts to

$$0.0000112 \times 0.036 \times 48 \times (1000)^2 = 20 \text{ tons.}$$

There are two straps each of section 8.3 centimetres $\times$ 1.25 centimetres to resist this force, so that the total section of the two straps is

$$8.3 \times 1.25 \times 4 = 41.5 \text{ square centimetres,}$$

as each strap offers two sections, 8.3 $\times$ 1.25 cms, to the stress.

The stress is

$$\frac{20}{41.5} = 0.48 \text{ ton per square centimetre.}$$

We must next consider the strips under the pole tips. The centrifugal force amounts to 2 $\times$ 20 = 40 tons. This force of 40 tons represents the total centrifugal force on the portions of the winding marked A.

As these portions are symmetrically placed, the force on the portion A must be equal to 20 tons, and may be represented by a radial vector mn.

This force may be resolved into two components parallel to, and at right angles to the axis of the pole, as indicated by the vectors mp and mq. Thus the pole tips have to resist a component of (20 cos 20°) tons, and the V brackets the remaining (20 sin 20°) tons.

Hence the force along the axis is equal to $20 \times 0.94 = 19$ tons. The thickness of the pole tips is 4.5 centimetres, and as one third of the length is taken up with ventilating spaces, the effective axial length at the pole tips is:

$$\frac{2}{3} \times 55 \times .95 = 35 \text{ centimetres.}$$

Therefore the effective section $= 4.5 \times 35 = 157$ square centimetres. The shear stress $= \dfrac{19}{157} = 0.12$ ton per square centimetre.

But we must also consider the bending moment caused by this force on the pole tips. We have a force of 19 tons uniformly distributed over 7.5 centimetres of the pole tip.

Hence the bending moment =

$$\frac{19 \times 7.5}{2} = 71 \text{ centimetre tons.}$$

But we also have the centrifugal force acting on the pole tips.

The breadth is $55 \times 0.95 \times \dfrac{2}{3} = 35$ centimetres, and the section $= 20$ square centimetres.

Therefore the weight is equal to

$$7.8 \times 10^{-6} \times 35 \times 20 = 0.0054 \text{ ton.}$$

As the mean radius $= 57$ centimetres, the centrifugal force =

$$0.0000112 \times 0.0054 \times 57 \times (1000)^2 = 3.45 \text{ tons.}$$

The inclination of this force to the axis of the pole is 15 degrees, consequently the force acting along the axis is

$$3.45 \cos 15° = 3.4 \text{ tons.}$$

This force may be assumed (though it is not strictly true) to be uniformly distributed over the pole tip.

Hence the bending moment =

$$\frac{3.4 \times 7.5}{2} = 12.8 \text{ centimetre tons.}$$

Total bending moment =

$$71 + 12.8 = 84 \text{ centimetre tons.}$$

Now the moment of inertia of the section =

$$\frac{35 \times (4.45)^3}{12} = 260,$$

and the stress induced by the bending moment of 84 centimetre tons =

$$\frac{84 \times 4.45}{260 \times 2} = 0.72 \text{ ton per square centimetre.}$$

The remaining component of the centrifugal force, i.e., that due to the pole tips, is equal to

$$3.45 \sin 15° = 0.9 \text{ ton.}$$

This force causes a tension in the pole tips. Their section is $35 \times 4.45 = 156$ square centimetres, and consequently the stress is equal to

$$\frac{0.9}{156} = 0.058 \text{ ton per square centimetre.}$$

We showed on page 314 that the component of the centrifugal force on the coil straps amounted to (20 sin 20°) tons = 6.9 tons.

But there is similarly a force of 6.9 tons due to the adjacent part of the winding of the next pole. This force is represented by the vector mq.

Hence considering any one angle bracket, we have the two forces mq, mq acting upon it at an angle of 120 degrees, and these two forces are equivalent to a single force qz of $2 \times \frac{6.9}{2} = 6.9$ tons acting radially. This is resisted by a section equal to

$$45 \times 1.25 = 56 \text{ square centimetres.}$$

Thus the resulting tensile stress is equal to

$$\frac{6.9}{56} = 0.123 \text{ ton per square centimetre.}$$

If, however, no angle brackets are provided, this stress would be resisted by a tensile stress on the copper winding: Thus we should have 300 turns of a section of 2.5×0.09 centimetres of copper, resisting a force of 6.9 tons.

The total section of copper is equal to

$$2.5 \times 0.09 \times 300 \times 2 = 133 \text{ square centimetres,}$$

and the stress =

$$\frac{6.9}{133} = 0.05 \text{ ton per square centimetre.}$$

The field windings, however, should be retained by brackets and straps, or their equivalent, in such a manner that the windings themselves are not subjected to any stress.

The above examples are typical of the calculations required for rotating fields with definite poles.

As we have previously mentioned, however, the present tendency in turbo-alternators is to use a distributed winding situated in the slots of a cylindrical laminated core.

As an immediate consequence of this arrangement the stress which is set up by the centrifugal force on the windings is considerably reduced, owing to the fact that it is no longer concentrated at certain points on the periphery, but is more or less uniformly distributed round the periphery in the slots. Furthermore, the very fact that the structure is in the form of a laminated core, permits the use of a somewhat higher safety factor, in view of the greater reliance which can be placed on the homogeneity of the material which necessarily follows from the method of manufacture.

We shall now briefly consider the principal stresses occurring in the 500-kva. turbo-alternator described and illustrated on pages 295 to 301 Chapter XI.

The actual calculations will be very similar to those given in Chapter XVIII for the stresses in the rotating armature of a continuous current generator. In both cases the windings are distributed in slots, and the slot portions are retained by wedges and the end portions by cylindrical covers. As the calculations are set forth at some length in that chapter, we shall in the present instance obtain the results rather more briefly. Following the method of procedure there indicated, we have

The Tension in the Stampings.

The weight of the copper winding	= 0.42 ton
and weight of the stampings	= 2.25 tons
So that the total weight	= 2.67, say 2.7 tons.

The mean radius is 21 centimetres, and the speed is $N = 1500$ revolutions per minute. The fundamental formula for the tension in cylinders due to centrifugal force is

$$F = \frac{0.0000112\, MRN^2}{\pi} = 0.0000036\, MRN^2,$$

so that the total pull is equal to

$$0.0000036 \times 2.7 \times 21 \times (1500)^2 = 460 \text{ tons.}$$

The minimum section resisting fracture occurs at a line drawn through the ventilating tunnels in Fig. 226.

The net length of the core is = 51.3, so the minimum section is equal to

$$51.3 \times 2 \times (2 + 7.5) = 51.3 \times 2 \times 9.5 = 975 \text{ square centimetres,}$$

and the average stress across this section

$$= \frac{460}{975} = 0.47 \text{ ton per square centimetre.}$$

Slots and Teeth. — The winding of the rotor is distributed in twenty-four slots, and the weight of copper embedded in each slot is equal to 0.009 ton.

The mean radius is equal to 27 centimetres, so that the centrifugal force is equal to $0.0000112 \times 0.009 \times 27 \times (1500)^2 = 6.1$ tons. This force is counteracted by the shear stress in the wedge; the depth of the wedge is 0.64 centimetre, and the length is 68.6 centimetres. Hence the total section resisting shearing is equal to

$$2 \times 0.64 \times 68.6 = 88 \text{ square centimetres,}$$

and the resulting stress is

$$\frac{6.1}{88} = 0.0\dot{6}9 \text{ ton per square centimetre.}$$

This would be much too high a stress for any kind of wood, so that a bronze of some kind would probably be used. In order to obtain

the tension at the root of the tooth, we must add to the centrifugal force of the copper, that due to the weight of the teeth themselves. The weight of one row of teeth is approximately 0.0072 ton.

Therefore the centrifugal force

$$= 0.0000112 \times 0.0072 \times 27 \times (1500)^2 = 4.9 \text{ tons,}$$

and the total pull on the roots of one row of teeth is equal to

$$6.1 + 4.9 = 11 \text{ tons.}$$

The width of the tooth at this point is 1 centimetre, and the net length of the core is to 51.3 centimetres.

Hence the total section is equal to 51.3 square centimetres, and the average stress amounts to

$$\frac{11}{51.3} = 0.214 \text{ ton per square centimetre.}$$

End Windings. — For simplicity we shall consider the end portions of the windings as being completely distributed round the periphery of the rotor. The weight of copper that is restrained by one of the end-bells is 0.1 ton so that the tension due to the centrifugal force acting on this copper is equal to

$$0.0000036 \times 0.1 \times 27 \times (1500)^2 = 22 \text{ tons.}$$

This tension is taken by the end-bell which has a cross section of 21 square centimetres. But the weight of the end-bell is equal to 0.034 ton, and the tension due to this centrifugal force on this mass amounts to

$$0.0000036 \times 0.034 \times 33 \times (1500)^2 = 9 \text{ tons.}$$

The total force tending to fracture the end-bell is

$$22 + 9 = 31 \text{ tons,}$$

and the total section resisting fracture is

$$2 \times 21 = 42 \text{ square centimetres.}$$

Hence the average stress in the end-bell amounts to

$$\frac{31}{42} = 0.74 \text{ ton per square centimetre.}$$

PART III — CONTINUOUS CURRENT GENERATORS.

CHAPTER XIII.

GENERAL CONSIDERATIONS RELATING TO THE INFLUENCE OF THE RATED OUTPUT, VOLTAGE, AND SPEED, ON THE DESIGN OF CONTINUOUS CURRENT GENERATORS.

We have already in Chapter I made some general observations on the suitability of the speeds of various types of prime movers for direct connection to continuous current generators. In the present and in the following chapters the general influence of the rated speed on the design will be dealt with in further detail.

In Table 42 is contained a list of prime movers and their approximate angular speeds, taken from the curves given in Fig. 1, for rated capacities of 250, 500, and 1000 kw. This practically covers the average range of outputs of continuous current generators.

TABLE 42.

Approximate Rated Speed in R.P.M, of a Number of Types of Prime Movers for Rated Capacities of 250, 500, and 1000 Kw.

Prime Mover.	250 Kw.	500 Kw.	1000 Kw.
Moderate speed steam engine	160	120	80
High speed gas engine	250	200	120
High speed steam engine	350	270	220
DeLaval steam turbine (speed of secondary shaft geared down in ratio of 10:1)	1000	...	...
Curtis type of steam turbine	2500	1800	1200
Parsons type of steam turbine	3000	2500	1900

In addition to the above types there must also be considered hydraulic turbines, the speed of which is controlled by the head of water available.

Even though the precise speed most favourable for the required continuous current generator may not be the same as that for the engine, it will generally be found that a wide considerable departure from this speed will not, in the case of the generator, be accompanied

by any prohibitive sacrifices in first cost or in quality of performance. This is seen from the curve of Fig. 230, which, for a 500-kw. 250-volt continuous current generator, shows the relation between the rated speed and the Total Works Cost of the generator. The curve shows that for this rated output and voltage the Total Works Cost is a minimum for about 1000 revolutions per minute. This is partly owing to the greater outlay for labour and for the additional structural material required for higher speed, and partly owing to the proportionately more lavish expenditure for "effective" material necessary to obtain at the higher speeds good performances from the commutating and thermal standpoint.

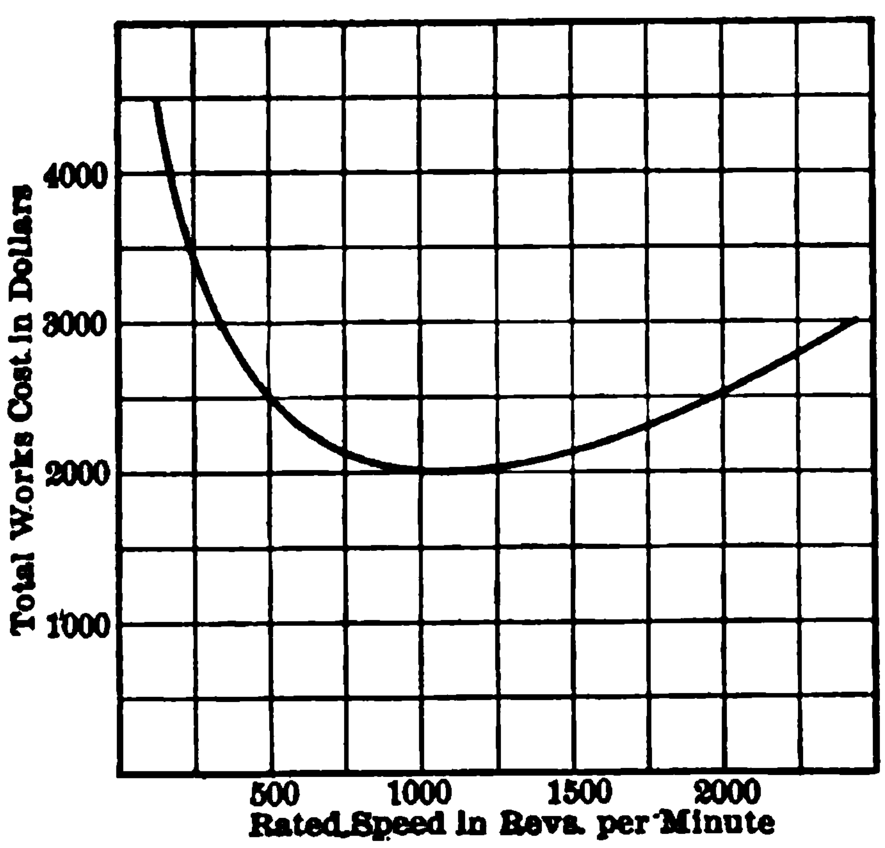


Fig. 230.— Total Works Cost of 500 kw. 250 volt continuous current generator for various rated speeds.

The cost of a continuous current generator of given quality is incapable of reduction by the employment of a higher speed for the design, to an extent at all commensurate with the increased speed. Within the ranges of speeds and capacities preferred by the makers of the so-called "high-speed" or "quick-revolution" engines, the dynamo builder is readily able to meet the requirements imposed by the speed preferred by the engine builder; and he will even then generally be working below the most economical speed associated with careful design. But for the large outputs for which the steam turbine is being so successfully developed, the speed required for the steam turbine greatly exceeds the most economical dynamo speed.

This has sometimes led to a sub-division of the continuous current dynamo, when required for such purposes, into two component machines, each for half the required output, for, as may be seen by Fig. 231, the smaller the rated capacity, the higher is the speed corresponding to the most economical design. This, however, is rather a crude arrangement, and by departing from the traditional methods of dynamo design, better solutions often become practicable. Where customary methods of design are adhered to, the speeds corresponding to minimum Total Works Cost for various rated outputs at 250 volts, may be taken as approximating to those plotted in the curve of Fig. 231.

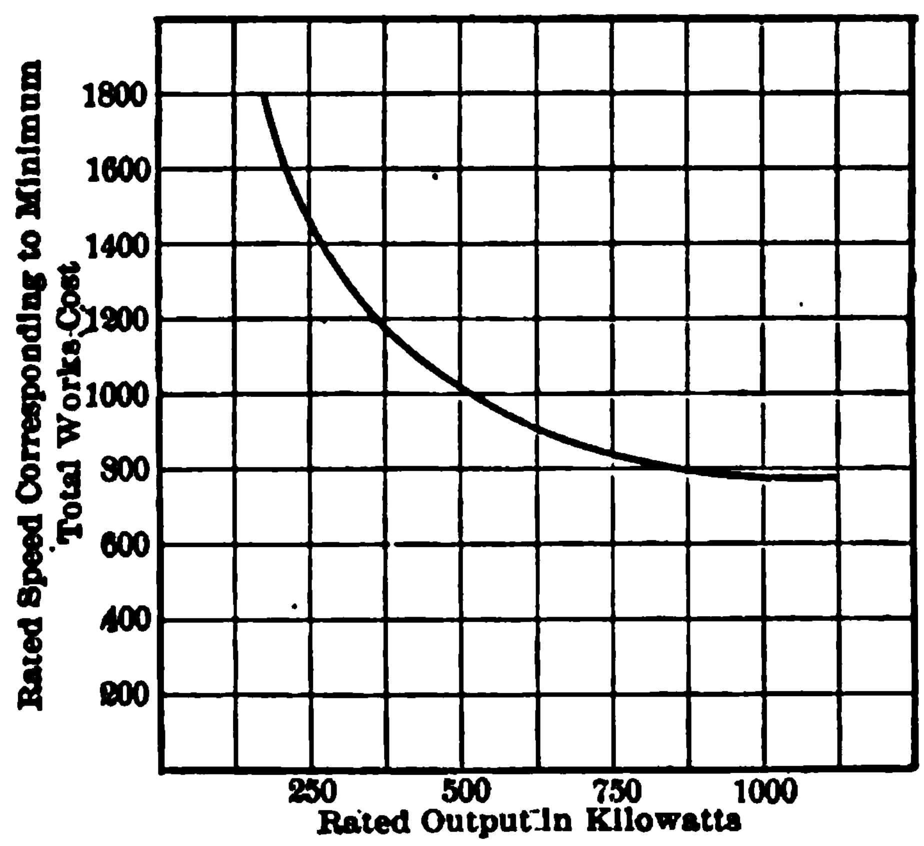


FIG. 231.— Curve of relation of minimum T.W.C. to speed for 250 volt continuous current generators.

Up to this point we have been considering only the *angular* speeds of machines. This is generally most conveniently expressed in revolutions per minute; but at any given instant, any point on the periphery of the armature, or rotor, has a definite *linear* speed, which may be expressed in metres per second. This is generally termed the *peripheral* speed of the armature or rotor. To avoid any misunderstanding we shall assume throughout this chapter that by the word "speed" when not otherwise qualified, we mean "angular speed" in revolutions per minute, and that when *linear speed at the periphery* is intended we shall always use the expression "peripheral speed," the unit being one metre per second.

In criticism of the contention that but small gain results from high speeds in continuous current dynamos, it will perhaps be pointed out that the list prices of dynamo builders show considerably greater decrease in prices with increasing rated speeds than would be expected in the light of the above remarks. This is attributable to a fundamental and widely spread error in the principles of dynamo design. Designers often employ, with more or less clear intention, not very greatly different peripheral speeds for dynamos of different angular speeds. This is incorrect. Machines which run at higher speeds should, from commutating considerations, be designed narrow and with a high peripheral speed, whereas low speed machines should be designed relatively wider and with proportionately lower peripheral speed. Thus for a given output and voltage, a dynamo may conveniently, for quite a wide range of rated speeds, have the same diameter, the width of laminations between end flanges being inversely proportional to the rated speed. By carefully determining for a given case, the most economical armature diameter and length for a given output, voltage, and speed, excellent designs for the same output and voltage may be derived for lower and higher speeds by changing the core length in inverse proportion to the speed. Thus the *peripheral* speed remains directly proportional to the *angular* speed. By applying these principles with care, not only may economical designs be obtained, but it will be found that whatever gain is made over existing good designs for high speed, is greatly exceeded by the gains made in the corresponding low speed designs. But the decrease in cost in going from the lowest to the highest speeds in the group thus designed, is much less than in machines for a corresponding range of speeds designed on the basis of constant peripheral speed for the group. This is illustrated by the curves of Fig. 232. The upper curve represents a group of machines for a given output and voltage, but for different angular speeds, all of which have the same peripheral speed. The lower curve is for corresponding machines designed on the basis of much the same diameter for all rated speeds, consequently of peripheral speeds decreasing approximately with the rated speed. If, as shown by the curve, the costs for both designs coincide at some high rated speed, then the designs at lower rated speeds will have a lower cost when designed on the principle of retaining much the same diameter and lengthening out the core in inverse proportion to the rated speed. But unless the correct diameter and length for a given rated speed, output, and

voltage are first carefully determined, then merely changing the length for other rated speeds — although in itself a correct principle — will nevertheless not result in an economical group of machines.*

These different methods of designing are illustrated in Fig. 233 by the diagrammatic outlines for 500-kw. 250-volt continuous current generators for rated speeds ranging from 100 to 500 R.P.M. The upper line of diagrams contains the outline plans for machines designed with absolutely constant armature diameter. The ideal design in this set, as regards diameter and gross core length, is the 300 R.P.M. design. The other designs have the same diameter, and differ from the ideal design only as regards the gross core length, which is altered

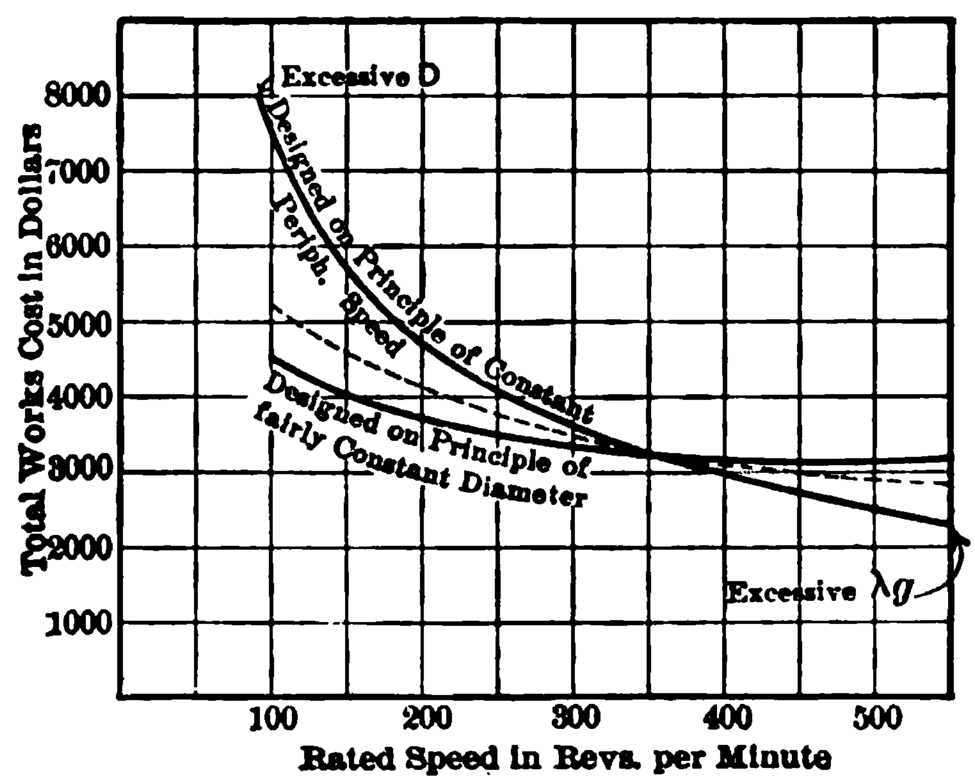


FIG. 232.— Total Works Cost curves for 500 kw. 250-volt continuous current machines.

in inverse proportion to the rated speed. This method of retaining the same diameter gives excellent results in the case of 200, 300, and 400 R.P.M. designs, but the 100 and 500 R.P.M. designs are so extreme that a moderate deviation from the rigorous principle leads to preferable designs.

A set of designs in which moderate deviations are made in these extreme cases, is indicated in the second line in Fig. 233. The 100 R.P.M. design has been increased in diameter and shortened in length, and the reverse procedure has been adopted for the 500

* In some cases where this *principle of design* has been correctly followed, the results have been comparatively uneconomical owing to starting from the basis of a machine of uneconomically small diameter and great length for its rated speed.

R.P.M. design. The 200, 300, and 400 R.P.M. designs retain the same dimensions as the first set. When the limit is reached beyond which it is not desirable to retain the same diameter, the change should, from standardising considerations, be abruptly made to the next larger or smaller standardised air gap diameter.

This second set of designs may be stated to be of "fairly constant" diameter. In the lower line of Fig. 233 the outlines of the designs for constant peripheral speed are given as illustrative of the incorrect principle. The low speed designs are of excessive diameter, and the high speed designs are of excessive length.

When one employs those principles of design leading to the greatest economy, the continuous current dynamo is, up to fairly large rated capacities, capable of being designed to give fairly satisfactory results as regards performance at high speeds. There is, however, for any given rated output and voltage, a limiting economical speed beyond which the cost again increases; and even below this economical speed, relatively little decrease in cost is attained by increased speed. Therefore the continuous current dynamo, in its present stage of development, contrary to the general belief, is inherently *not* a high speed, but a low speed machine. The difficulties encountered at high angular speeds, relate partly to thermal considerations, but chiefly to commutation; hence alternating current dynamos are much better adapted than continuous current dynamos to the speeds of large steam turbines; and at such speeds, the designs for alternating current dynamos work out to be very low in cost, though even there a definite speed can be found for any given rated output, which is the most economical for given conditions, and beyond which the cost will increase with further increase in speed.

The principle of designing continuous current generators for a given rated capacity and voltage, but for varying rated speeds, with a fairly constant diameter, reaches a limit at low rated speed, where thermal considerations require that the diameter should be increased and the length decreased. It also reaches a limit at high rated speeds where mechanical considerations require a reduction in the diameter notwithstanding the electromagnetic and thermal disadvantages thereby entailed. These limitations will be considered in detail in later sections.

Until very recently there existed, even amongst the largest and most experienced electrical manufacturing companies, the most

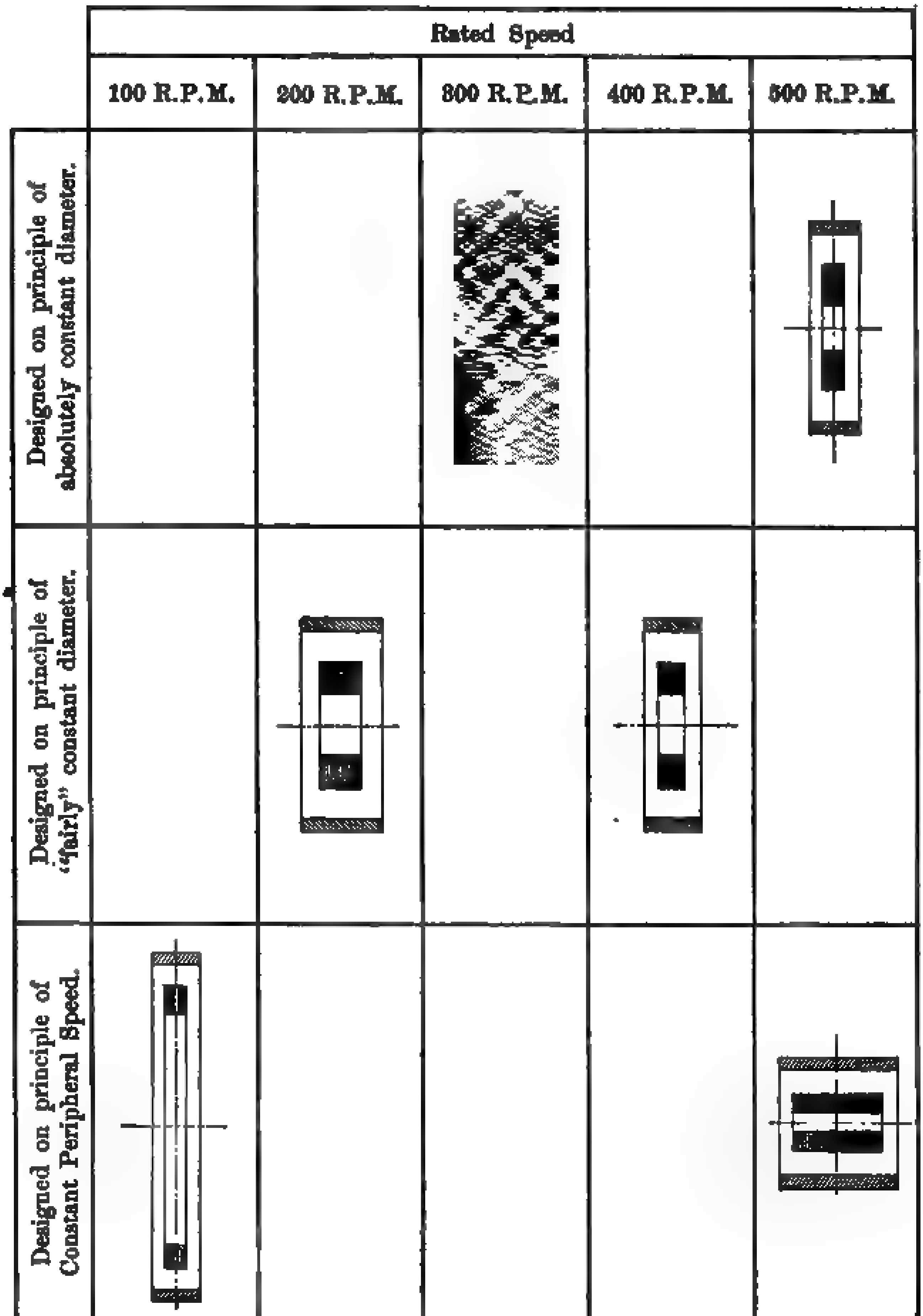


FIG. 233.—Sketches showing outlines of 500 kw. 250 volt machines for various rated speeds and designed on three different principles.

complete failure to appreciate the difficulties of designing continuous current dynamos for steam turbine speeds. Some seven years ago one of the very largest manufacturers of dynamo electric machinery light-heartedly took in hand the *standardisation* of continuous current steam turbine-driven sets of sizes running up to 2000 kw. rated capacity. Nevertheless, at the present time (some seven years have elapsed), there are still but very few instances of continuous current steam turbine-driven sets of much over 500 kw. rated capacity, except where two dynamos are connected to a single steam turbine. 1000 kw. sets are now occasionally in evidence, and 2000 kw. sets are also, in one or two instances, being installed. A main reason for the failure to develop large sizes of continuous current turbo-generators relates to the fact that the rational method of design based on the employment of widely varying peripheral speed as above outlined, for continuous current dynamos of only moderately high speeds, fails when applied to dynamos for large rated output and the far higher speeds common to steam turbines. The method fails when the peripheral speed limitations of commutators are reached. Carbon brushes give excellent satisfaction when employed on copper commutators running at peripheral speeds below 16 metres per second. From this point up to 26 metres per second, the result may or may not be satisfactory according to the accompanying diameter, the excellence of the mechanical design of the commutator and of the electrical design of the whole machine. In a general way, of two commutators of the same *peripheral speed*,, the design and construction of that one having the *greatest diameter* and the *lowest angular speed*, will the most readily lead to good results. This is obvious, since we have:

$$\text{Centrifugal force} = 0.00000559\ DR^2$$

where

$$D = \text{diameter in centimetres}$$

and

$$R = \text{angular speed in revolutions per minute.}$$

The centrifugal force given by the above formula is that at the periphery, and it is expressed in kilograms per kilogram of rotating material.

If by S we denote the peripheral speed in metres per second, then

$$S = \frac{\pi D}{100} \times \frac{R}{60} = 0.000525\, DR.$$

$$\text{Consequently,}\quad R = \frac{S}{0.000525D}.$$

$$\text{and Centrifugal force} = \frac{0.00000559DS^2}{0.000000275D^2} = 20.3\,\frac{S^2}{D}.$$

Hence, for a given peripheral speed, a commutator will be less liable to high bars or to mechanical distortion of any sort, the greater the diameter. Substituting for D, from the formula

$$D = \frac{S}{0.000525R},$$

we have

$$\text{Centrifugal force} = 0.0106\, SR,$$

and we can extend the preceding statement to read as follows:

"A commutator for a given peripheral speed will be less liable to high bars or to mechanical distortion of any sort, the greater the value of D, the diameter in centimetres, or the less the value of R, the speed in revolutions per minute."

It is necessary to emphasise this very elementary point, because it is too often considered that the peripheral speed, taken alone, affords a criterion to the mechanical practicability of a given design of commutator. It also explains why a higher peripheral speed may successfully be employed the lower the angular speed R of the design. At and beyond 26 metres per second, carbon brushes on copper commutators of less than very large diameters are generally very troublesome, but considerably higher peripheral speeds are nevertheless often employed in the larger turbine driven dynamos, for with a decreased diameter it would be impracticable to obtain the extent of radiating surface necessary for avoiding undue temperature rise except by so greatly increasing the length of the commutator as to entail mechanical difficulties as regards rigid construction, and cumulative thermal difficulties in consequence of the lack of space for internal air passages through the commutator for ventilating purposes.

Instead of carbon brushes, some firms employ copper for the brushes of their turbine dynamos, and commutator peripheral speeds

of some 40 metres per second have sometimes been employed in such cases. These questions are treated in considerable detail in Chapter XIX.

In the following chapter the commutation limitations encountered with high speed dynamos are defined. For the high reactance voltages encountered in this class of work, the ordinary construction without interpoles or compensating winding is utterly insufficient, and one of two additional means is customarily employed.

The first of these means consists in providing a set of small poles between the main poles as indicated in Fig. 294. These small poles are termed "interpoles," and their function is to provide a magnetic flux suitable, as regards quantity and location, to approximately neutralise the reactance voltage which would otherwise set up objectionable currents in the coils undergoing commutation, with the results that there would be sparking at the commutator. The design of interpoles is dealt with in another chapter. At this point it is desirable to state that the provision of interpoles, while necessary for high speed continuous current dynamos in the interests of obtaining good commutation, is, from all other standpoints, distinctly disadvantageous. The leakage factor is larger in interpole designs, and to prevent it from being unduly large, a greater air gap diameter or a smaller number of main poles must be employed than would otherwise be most suitable. The direct cost of the material and labour associated with the interpoles and their windings, together with the greater cost inherent to a larger diameter or fewer poles, results in a materially more expensive machine. The additional I^2R loss in the interpole windings entails additional heating and lower efficiency. The crowding of interpoles in between the main poles is in itself a cause of increased heating of the main coils, as it interferes with the circulation of the air currents. When, in addition to this circumstance, the additional loss in the interpole windings is taken into account, the disadvantages of interpole designs from the thermal standpoint are found to be very considerable.

It is often the case that in order to find room for the interpoles and to avoid an excessive magnetic leakage factor, the main magnet cores which might otherwise have been designed of circular cross section, or nearly so, have to be designed with a section more or less elongated in the direction of the shaft. This entails a greater weight of copper for the main field spools, greater loss therein, and impedes

the circulation of the air required for restricting the temperature rise consequent upon this and other losses.

The weight of copper on a spool for a given number of ampere turns and heating (watts per square decimetre of surface) is proportional to the mean length of a turn.

The following is a simple rational relation for field spools:

$$W = CD \times AT \times l \times \rho,$$

where W = watts lost in the coil.

CD = current density in amperes per square centimetre.
AT = ampere turns.
l = mean length of turn in centimetres.
ρ = specific resistance of copper (= 0.000002 ohm per centimetre cube at 60 degrees C.).

For a given number of ampere turns and current density (i.e., cross section of winding space), the watts lost are proportional to the mean length of turn. If the length of the spool does not change, the external surface is practically proportional to the mean length of turn, and since, as above stated, the watts lost are proportional to the mean length of turn, the watts per square decimetre of surface are practically constant and independent of the shape of the section of the pole. The weight of copper is, however, proportional to the mean length of turn which is approximately proportional to the length of the internal periphery of the spool, at least when the depth of winding is moderately small as compared with the internal diameter of the spool. The perimeter depends on the shape of the section of the magnet pole, and varies with the proportions of the section for a given area, A^2, in the manner indicated in the table in Fig. 234.

The weight of copper for a given number of ampere turns and a given magnetic cross section of the pole is a minimum when the pole is of circular section, and if of rectangular section, the weight is smaller the nearer the shape of the section approximates to a square. The practicable shape of the pole section is dependent on the ratio of the armature gross core length λ_g to the pole pitch τ and, for a pole core of circular section, this ratio has a value of from 0.35 to 0.65. These values follow from the average relation between the breadth of the pole arc, a, which averages 0.7 of τ and the breadth of pole

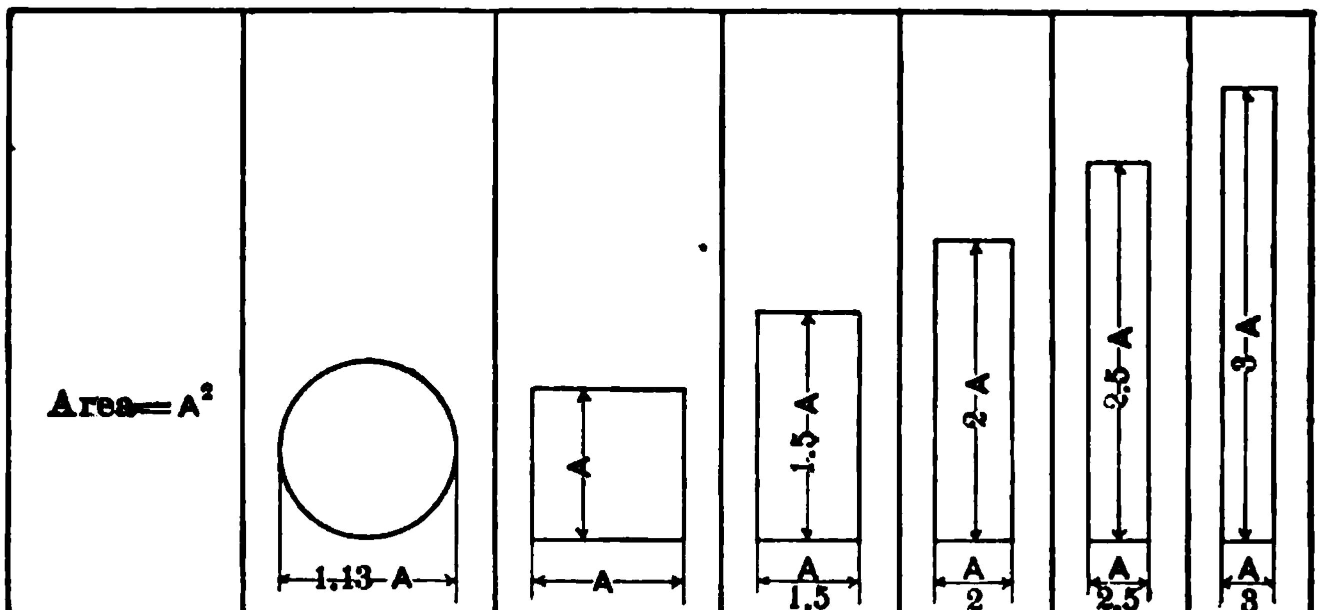
core b, which is from 0.5 to 0.9 of the pole arc, from consideration of the proportioning of the dimensions of the magnetic circuit for appropriate flux densities.

$$\text{Hence} \qquad \frac{b}{\tau} = (0.5 \times 0.7) \text{ to } (0.7 \times 0.9).$$

or, say, 0.35 to 0.65.

If the pole is of square or circular section, $b = \lambda_g$* (as these are the dimensions of the pole section), and hence

$$\frac{\lambda_g}{\tau} = 0.35 \text{ to } 0.65.$$



Perimeter	3.55 A	4 A	4.5 A	5 A	5.8 A	6.66 A
Perimeter in terms of circumference of circle	1.0	1.13	1.27	1.41	1.63	1.88

Fig. 234.— Magnet pole sections and perimeters.

That is to say, the amature gross core length varies from 0.35 to 0.65 of the pole pitch. With interpole designs, in order to obtain room for the interpoles, and to keep down the magnetic leakage, the pole arc is preferably less than 0.7 of the pole pitch, and hence the value of $\frac{\lambda_g}{\tau}$ will be less than 0.5, which leads, as noted above, to large pole pitches and armature diameters.

* This is on the assumption that the pole diameter or breadth of the pole core parallel to the shaft is equal to the armature gross core length. In many cases in practice the pole diameter is rather less than λg (some 80 to 90 per cent) and, in a few cases, slightly greater. For the purposes of the present general analysis we may take the pole core diamater equal to the armature gross core length, or $b = \lambda g$.

By way of illustration, the curve in Fig. 235 shows the increasing outlay for copper when elongated rectangular coils are employed for a given case, instead of circular coils. The assumptions underlying this curve are set forth in the following paragraphs :—

In working out the data for the curve of Fig. 235 it is assumed that a magnet core with a section of 1000 square centimetres is required to carry the flux, and a magnetomotive force of 10,000 ampere turns, to induce that flux. The depth of the winding is taken

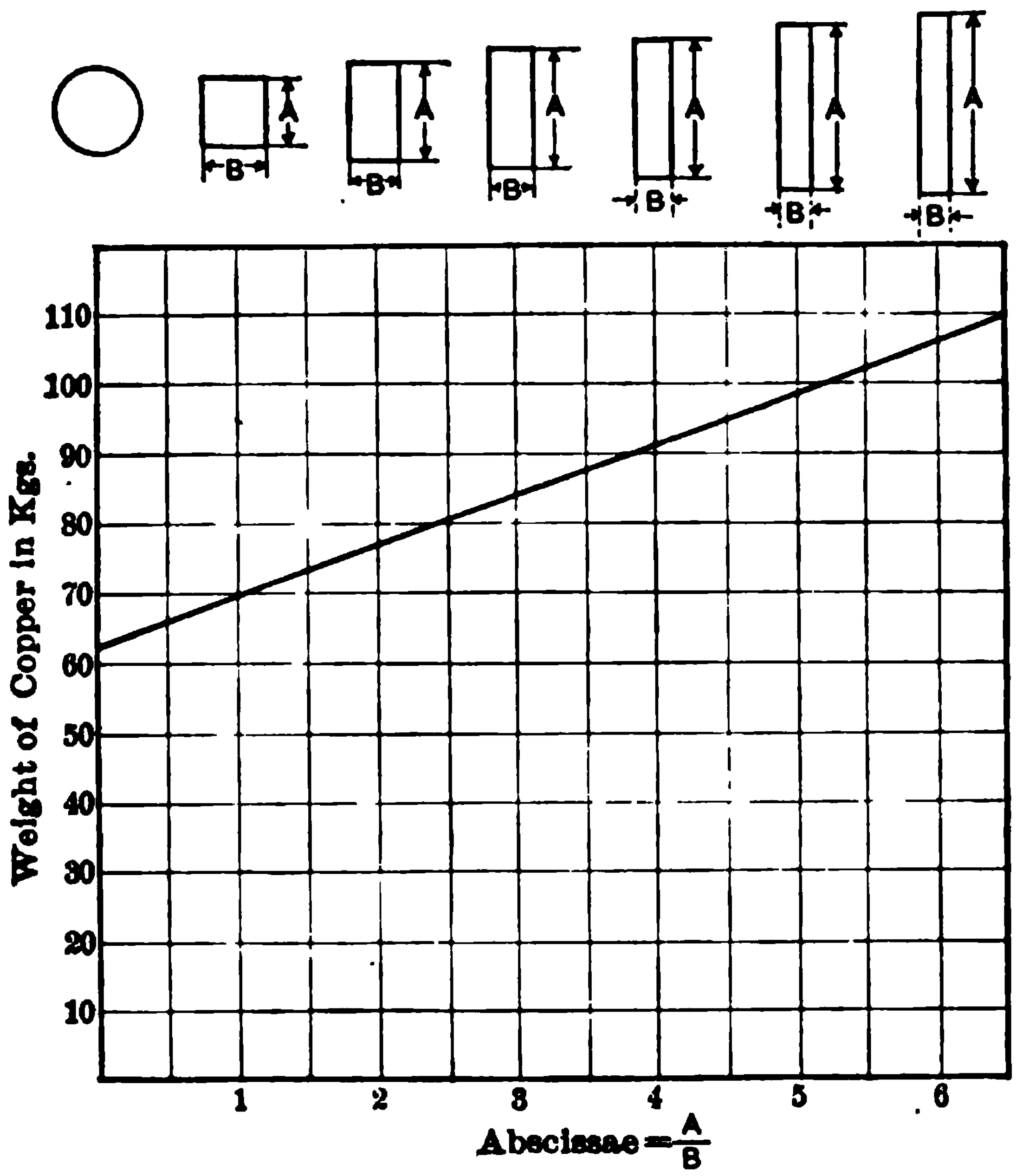


Fig. 235.— Curve showing the effect of the shape of the magnet core on the weight of the magnetising copper required.

equal to 5 centimetres for all cases. The voltage across the coil is taken at 100 volts, and the heating coefficient allowed is 9.5 watts per square decimetre. It is interesting to notice that the required length of the coil is found to be practically the same (25 centimetres) for every case, showing that the section of the core affects only the weight of the copper, and not the length of the magnet core, which is in accordance with the above reasoning.

In order that the curve in Fig. 235 may serve as a rough guide for field spools with other constants, it may be noticed at this point

that the weight of copper varies inversely as the loss in watts, and approximately directly as the diameter of the magnet core. If the spool is made of greater radial length and with a smaller winding depth, the weight of copper is reduced, but to effect this, the weight of the magnet cores and yokes is increased. It is difficult to lay down any rule for this, but it may generally be assumed that the winding depth should be kept down to some four or five centimetres, though this must be governed entirely by the type and size of each machine and the conditions for which it is designed. The labour attending the winding of field spools is also less the nearer the approach to a circular form, and the machining operations on the magnet core are simpler.

Interpoles proportioned to neutralise the reactance voltage do not decrease, but, on the contrary, may somewhat increase the armature distortion. To overcome this defect, compensating windings are often employed. These consist in windings distributed over the pole face and directly opposite the armature windings. If the main current after leaving the armature is carried through such compensating windings, the armature interference may be completely neutralised at every point. These distributed windings may also serve to excite the interpoles, and in such cases the interpoles do not require to be provided with spools. A design on these lines is illustrated in Fig. 310 on page 429.

This is the ideal solution for extreme cases where considerations of size and cost must give way to considerations relating to obtaining the best possible performance as regards commutation. The great majority of cases where the ordinary design without auxiliary features fails to suffice, may, however, in the authors' opinion be appropriately met by interpole designs. Beyond the limits where good results are certain of attainment with interpole designs, advantage must be taken of the further improvement incident to employing compensating windings in addition to interpoles. Some firms, however, employ the combination of interpoles and compensating windings even in designs of low ratings. It would appear that they thereby not only needlessly increase the cost, but sacrifice the efficiency and impair the ventilation.

The number of high speed continuous current dynamos of really large output which have as yet been put to work, is still far from great, and actual experience with them covers so short a period that at the

present time no one is justified in expressing any very emphatic opinions.

It is at just such a stage of development, however, that such a study of the subject as that which the authors set forth in the following pages, is the more needed, and it is with a realisation of this circumstance that we have gone exhaustively into the preparation of an extensive series of tentative designs. Our programme has been too extensive to permit of working out each single design in detail, and no one should commit the indiscretion of proceeding to build any particular design employing the dimensions set forth in this treatise. Nevertheless, as a good preliminary basis for detailed study, these individual designs should prove useful. Our purpose has related chiefly to investigating wide ranges of conditions with a view to arriving at the natural laws underlying the subject.

The attainment of this purpose does not require a complete exposition of the elementary principles of dynamo design. These principles are set forth in various text-books to which the reader should refer, and they lead up to various useful rules and empirical methods which constitute most suitable starting-points for such investigations as are undertaken in the present treatise.

CHAPTER XIV.

A METHOD OF DETERMINING THE LEADING DIMENSIONS OF LARGE, HIGH SPEED, CONTINUOUS CURRENT GENERATORS.

An important consideration in the determination of the leading dimensions and general design of a continuous current dynamo is that of commutation. With high speed continuous current dynamos this consideration constitutes the most important limitation of the output.

The Estimation of the Reactance Voltage. — For the purposes of the present treatise it is most convenient to estimate Ω, the reactance per pole in ohms, as follows:

$$\Omega = K \times \lambda_g \times R \times F \times 10^{-8}, \quad \ldots \ldots \quad (1)$$

where K is a factor obtained from the curve in Fig. 236.

λ_g = gross core length in centimetres.
R = speed in revolutions per minute.
F = total number of face conductors on armature.
τ (in Fig. 236) = polar pitch at air gap in centimetres.
τ is equal to $\frac{\pi \times D}{P}$ where D = gap diameter in centimetres,

and P = number of poles.

This formula, it should be stated, is, as it stands, only correct for multiple circuit single windings with one turn per commutator segment. It is, however, exclusively with machines employing this type of winding, that we shall in the present treatise have to deal. If this type of winding is employed, then, when the dynamo is loaded with a current of I amperes, the current per circuit is equal to $\frac{I}{P}$. Hence, the reactance voltage is equal to $\frac{I}{P} \times \Omega$.

Thus we have

$$\text{Reactance voltage} = K \times \lambda_g \times R \times F \times \frac{I}{P} \times 10^{-8} \quad . \; . \quad (2)$$

Furthermore,

$$\text{Armature ampere turns per pole} = \frac{F}{2 \times P} \times \frac{I}{P} = \frac{1}{2 \times P} \times \frac{F \times I}{P}.$$

$$\text{Therefore } \frac{F \times I}{P} = 2 \times P \times \text{armature ampere turns per pole}. \; . \quad (3)$$

Substituting (3) in (2) we obtain

$$\text{Reactance voltage} = 2 \times K \times \lambda_g \times R \times P \times \text{armature ampere turns per pole} \times 10^{-8} \; . \; . \; . \; . \; . \; . \; . \quad (4)$$

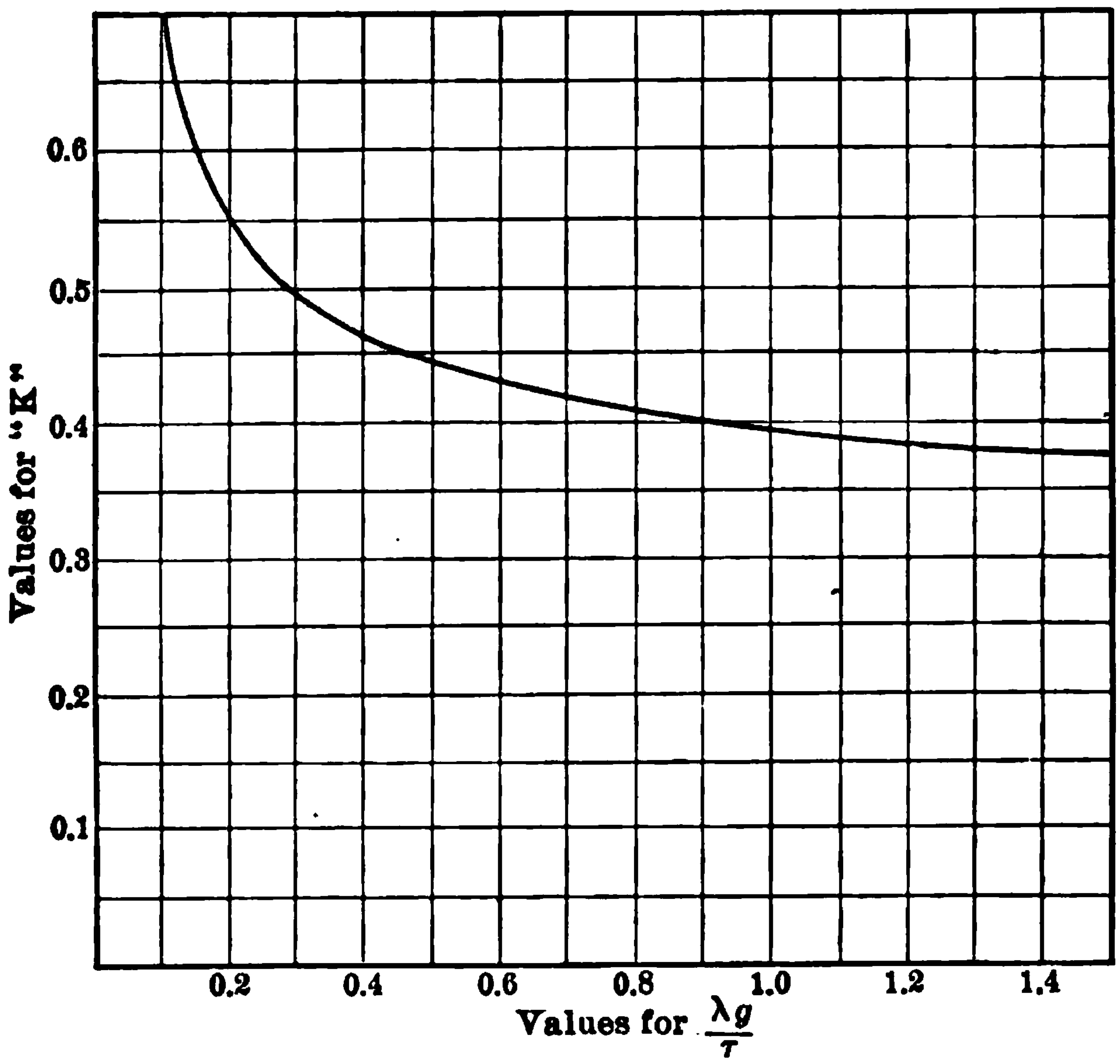


FIG. 236.— Curve showing the value of "K" in the formula, $\Omega = $ "K" $\times \lambda g \times R \times F \times 10^{-8}$, for estimating the reactance voltage.

When, as will generally be the case, greater exactness is not warranted, we may substitute for K the mean value of 0.4, and our formula for the reactance per pole will then read

$$\Omega = 0.4 \times \lambda_g \times R \times F \times 10^{-8}. \; . \; . \; . \; . \; . \; . \quad (5)$$

and the formula for the reactance voltage will then read

$$\text{Reactance voltage} = 0.8 \times \lambda_g \times R \times P \times \text{armature ampere turns per pole} \times 10^{-8} \; . \; . \; . \; . \; . \; . \; . \quad (6)$$

Thus for various numbers of poles we obtain for the reactance voltage the expressions brought together in Table 43.

TABLE 43.

EXPRESSIONS FOR THE REACTANCE VOLTAGE OF MULTIPLE-CIRCUIT WINDINGS WITH VARIOUS NUMBERS OF POLES.

Reactance Voltage of Multiple Circuit Windings.	
Number of Poles.	Formula.
4	Reactance Voltage = $3.2 \times \lambda_g \times R \times$ Arm. a't's per pole $\times 10^{-8}$
6	Reactance Voltage = $4.8 \times \lambda_g \times R \times$ Arm. a't's per pole $\times 10^{-8}$
8	Reactance Voltage = $6.4 \times \lambda_g \times R \times$ Arm. a't's per pole $\times 10^{-8}$
12	Reactance Voltage = $9.6 \times \lambda_g \times R \times$ Arm. a't's per pole $\times 10^{-8}$
16	Reactance Voltage = $12.8 \times \lambda_g \times R \times$ Arm. a't's per pole $\times 10^{-8}$

Each of these expressions may be simplified for the case of a given number of armature ampere turns per pole. Thus in Table 44 are given the expressions obtained for various armature strengths.

TABLE 44.

FACTORS TO BE EMPLOYED IN OBTAINING THE REACTANCE VOLTAGE IN TERMS OF THE GROSS LENGTH OF THE ARMATURE CORE, AND THE SPEED IN REVOLUTIONS PER MINUTE.

Armature Strength in Ampere Turns per Pole.	Values of K in the Formula Reactance Voltage $= K \times \lambda_g \times R$ for the following Numbers of Poles.							
	4	6	8	10	12	14	16	20
12,000 . . .	.000384	.000576	.000768	.000960	.001152	.001340	.001536	.001920
11,000 . . .	.000352	.000528	.000704	.000880	.001056	.001230	.001408	.001760
10,000 . . .	.000320	.000480	.000640	.000800	.000960	.001120	.001280	.001600
9,000 . . .	.000288	.000432	.000576	.000720	.000864	.001010	.001152	.001440
8,000 . . .	.000256	.000384	.000512	.000640	.000768	.000900	.001024	.001280
7,000 . . .	.000224	.000336	.000448	.000560	.000672	.000785	.000996	.001120
6,000 . . .	.000192	.000288	.000348	.000480	.000576	.000672	.000768	.000960
5,000 . . .	.000160	.000240	.000320	.000400	.000480	.000560	.000640	.000800
4,000 . . .	.000128	.000192	.000256	.000320	.000384	.000448	.000512	.000640
3,000 . . .	.000096	.000144	.000192	.000240	.000288	.000336	.000384	.000480
2,000 . . .	.000064	.000096	.000128	.000160	.000192	.000224	.000256	.000320

It is thus evident that for a given number of poles P, a given armature strength in ampere turns per pole, and a given speed, R, in

revolutions per minute,* we may plot the reactance voltage as a function of λ_g the gross core length.

Let us apply this process to the case of 6-pole machines for a speed of 1000 revolutions per minute.

$$P = 6; \qquad R = 1000.$$

In Fig. 237, the five curves relate to designs with armature strengths of 2000, 4000, 6000, 8000, and 10,000 ampere turns per pole, and conform fairly with the 6-pole formula in Table 43. This formula reads:

Reactance voltage $= 4.8 \times \lambda_g \times R \times$ Armature ampere turns per pole.

As Fig. 237 relates exclusively to designs for a speed of 1000 R.P.M. the formula becomes

Reactance voltage $= 4800 \times \lambda_g \times$ armature ampere turns per pole $\times 10^{-8}$.

The lowest of these five curves, which represents designs with 2000 armature ampere turns per pole, is, except toward the left hand end, closely represented by the expression

$$\text{Reactance voltage} = 0.096 \times \lambda_g.$$

To be precise, however, the curves in Fig. 237 are plotted with the varying values of K corresponding to the curve in Fig. 236, whereas Tables 43 and 44 are based on the *mean* value of K, namely, 0.4. Hence for the low values of λ_g, *i.e.*, for the left hand ends of the curves

* In order to obtain the value of $\frac{\lambda_g}{\tau}$, the further assumption has been made that there are 150 armature ampere turns per centimetre of gap periphery. The influence of this additional assumption will be discussed at a later stage. The assumption is only necessary when in the formula

$$\text{Reactance voltage} = K \times \lambda_g \times R \times F \times \frac{I}{P} \times 10^{-8}$$

we wish to employ the precise values of K given by the curve in Fig. 236. When, for K, we substitute the mean value of 0.4, we may plot fairly correct equivalents of the curves of Fig. 237. Before, however, we can assign a diameter to each curve as shown in Fig. 238, this assumption as to the ampere turns per centimetre of periphery, must be made. The curves plotted from the formula

$$\text{Reactance voltage} = K \times \lambda_g \times R \times \text{Arm. a't's per pole}$$

would have differed from those actually given in Fig. 237 in that they would, if extended, have all passed through zero, whereas the curves in Fig. 237 cut the axis of ordinates at points appreciably above zero. These considerations are more fully set forth in the immediately following pages, and this footnote has been inserted merely to save the reader from undue concern regarding inconsistencies for which, in the end, he will see the justification.

of Fig. 237, where the ratio of $\frac{\lambda_g}{\tau}$ becomes small, the reactance voltage is appreciably higher than is given by the formula

$$\text{Reactance voltage} = 0.096 \times \lambda_g.$$

In fact, for the limiting (and imaginary) case of a machine in which the diameter remaining constant, the gross core length becomes equal to zero, *i.e.*, in which $\lambda_g = 0$, there is still, for a given armature strength, a certain reactance voltage due to the inductance of the end connections. This is the reason why the curves in Fig. 237, instead of approaching zero with decreasing values of λ_g, cut the axis of ordinates at points considerably above zero.

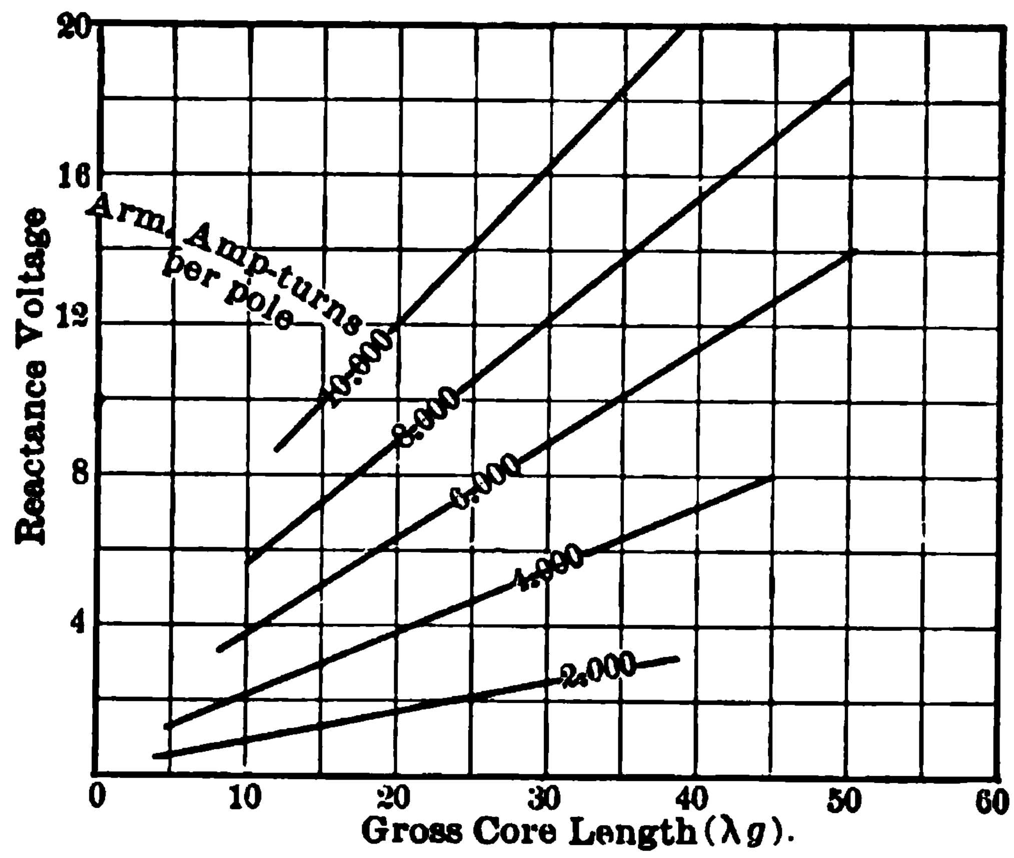


FIG. 237.— Reactance voltage curves for various 6-pole 1000 R.P.M. designs for various armature strengths.

While it has been desirable to recognise this in plotting the curves in Fig. 237, it is quite needless for the designer to unduly burden his mind with the fact, since the reactance voltage is not a quantity of such a definite nature as to be susceptible of predetermination with much accuracy. Thus, for general purposes, the reactance voltage for a given armature strength (in ampere turns per pole), and for a given speed and number of poles, may be taken as directly proportional to λ_g as set forth in Table 44.

In order to plot the curves in Fig. 237 on this more exact basis, a certain value for the armature ampere turns per centimetre of gap periphery had to be assumed in order to be able to determine the diameter as a step in the determination of $\frac{\lambda g}{\tau}$. The average value of

150 armature ampere turns per centimetre of gap periphery was taken. Thus for the curve for 2000 armature ampere turns per pole ($6 \times 2000 = 12,000$ ampere turns for the entire periphery), the gap diameter is obtained as follows:

$$D = \frac{12,000}{150 \times \pi} = 25.4.$$

In the same way the gap diameters for the other curves are obtained, and from these the values of S, the peripheral speed in metres per second, and of τ, the polar pitch in centimetres.

Thus $$S = \frac{1000}{60} \times \frac{\pi D}{100} = 0.525 \times D,$$

and $$\tau = \frac{\pi \times D}{6} = 0.525 \times D.*$$

These values are brought together in table 45:

TABLE 45.

VALUES OF ARMATURE STRENGTH AND ARMATURE DIAMETER.

Armature Strength in Ampere Turns per Pole.	D (Gap Diameter in Centimetres).	S (Peripheral Speed in Metres per Second).	τ (Polar Pitch at Air Gap in Centimetres).
10,000	127	67	67
8,000	102	53	53
6,000	76	40	40
4,000	51	26	26
2,000	25	13	13

To plot any one of the five curves of Fig. 237, say the curve for 6000 armature ampere turns per pole, it has merely been necessary to proceed as in Table 46.

TABLE 46.

VALUES OF ARMATURE GROSS CORE LENGTH AND REACTANCE VOLTAGE.

λ_g.	τ. (From Table 45).	$\frac{\lambda_g}{\tau}$.	K (from Fig. 236).	Reactance Voltage
10	40	0.25	0.525	3.8
20	40	0.50	0.44	6.4
30	40	0.75	0.42	9.1
40	40	1.00	0.39	11.3
50	40	1.25	0.38	13.9

The other curves are similarly derived.

* For 6-pole 1000 R.P.M. designs, the value of τ in centimetres happens to be equal to the peripheral speed in metres per second. This would, of course, not be the case for other numbers of poles or speeds.

It must be understood, however, that the armature ampere turns per centimetre of periphery is a quantity for which widely varying values may all lead to good designs. Thus, instead of the average value of 150 armature ampere turns per centimetre, 125 or 175 and even more widely diverging values might, in a given case, be employed, the magnetic flux being in the one case proportionately greater, and in the other case proportionately less.*

Thus, instead of designating these five curves as corresponding respectively to 2000, 4000, 6000, 8000, and 10,000 armature ampere turns per pole, it is preferable to designate them as *a*, *b*, *c*, *d* and *e*.

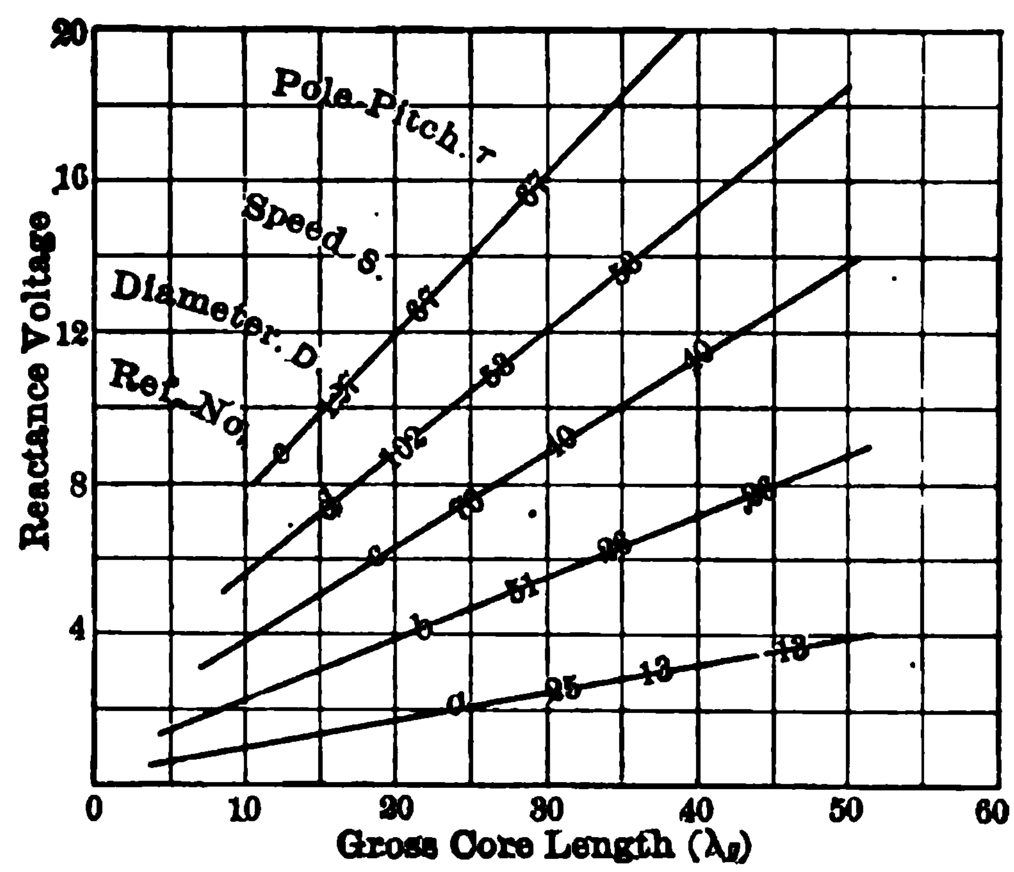


FIG. 238.— Reactance voltage curves for 6-pole 1000 r.p.m. designs for various diameters.

It will be more convenient on the diagrams to denote the lines for armature strengths of 2000, 4000, 6000, 8000 and 10,000 ampere turns per pole, *a*, *b*, *c*, *d*, and *e* respectively. In the present treatise we are exclusively concerned with machines of rather large rated outputs, these rated outputs rarely lying outside of the limits of 250 kw. as a minimum and 1000 kw. as a maximum. For this range of rated outputs, a loading of 150 ampere turns per centimetre of gap periphery is a sufficiently representative value for our purpose. On the basis of this value and for a design with a given number of poles,

* The values of the armature ampere turns per centimetre of gap periphery increase gradually from very small values in machines of small capacity or size, up to well on toward 200 in very large machines, and would often be higher still for low voltage machines of large rated capacity. (See Fig. 239).

a definite diameter is associated with each armature strength. Thus for 6-pole designs with 150 armature ampere turns per centimetre of gap periphery, curves a to e have the values for τ and D, which are written against them in Fig. 238. If, furthermore, the designs are all for a rated speed of 1000 R.P.M., i.e., if we have

$$R = 1000,$$

then values for S, the peripheral speed of the armature, may also be written against the curves as has been done in Fig. 238.

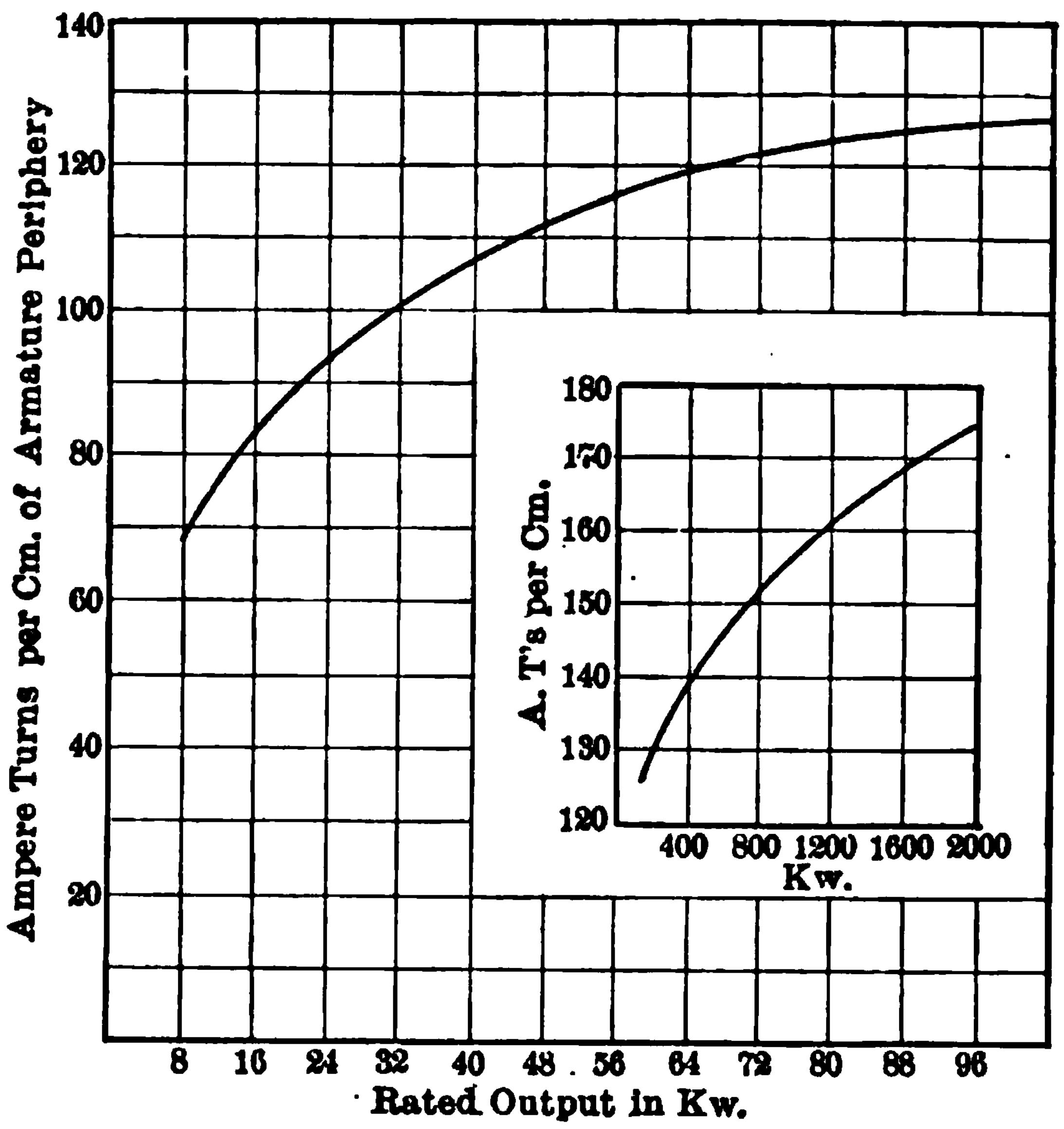


FIG. 239.— Curves showing suitable armature ampere turns per Centimetre of periphery for continuous current machines for 500 volts.

In the interests of arriving at a method of continuous current dynamo design which shall permit of obtaining a broad and well-balanced understanding of the influence of the most important factors, it is not only justifiable, but essential, to make assumptions such as this of 150 ampere turns per centimetre of gap periphery, notwithstanding a full realisation of the fact that widely diverging values of this quantity must often be substituted in the process of working out individual designs.

In certain instances a reference to Table 47 showing values of τ corresponding to curves a to e for other armature loadings than

150 ampere turns per centimetre of gap periphery, is convenient. In the actual detailed designing of particular machines, it is often useful, for the purposes of preliminary assumptions, to consult the curves of Fig. 239 in which fair representative values of the armature ampere turns per centimetre of periphery for 500-volt machines are plotted as a function of the rated output in kilowatts.

The curves of Fig. 239 enable us to readily correct the reactance voltage values as taken direct from the curves. Thus for the case of a design employing only 100 ampere turns per centimetre of gap periphery, the reactance voltage for a given pair of values of D and λg will be only

$$\frac{100}{150} \times \text{Ordinates to curves in Figs. 237 and 238.}$$

The best practice in dynamo design requires lower values for the armature ampere turns per centimetre of gap periphery, for higher voltages, and higher values for lower voltages. We shall also learn in later sections of the treatise that these values are also affected by the rated speed. It will be noted that for 250 kw. rated output, the plotted value is some 133 ampere turns per centimetre of gap periphery as against a value of about 157 for designs for a rated output of 1000 kw. For the broad purpose which we have at present in hand, the representative value of 150 armature ampere turns per centimetre of gap periphery is exclusively employed.

TABLE 47.

VALUES OF τ FOR VARIOUS CURVES IN FIGS. 238 AND 247, AND FOR VARIOUS VALUES OF ARMATURE AMPERE TURNS PER CENTIMETRE OF PERIPHERY AT AIR GAP.

Ampere Turns per Cm. of Periphery.	Values of the pole pitch τ for Curves in Figs. 238 and 247.				
	a.	*b.*	*c.*	*d.*	*e.*
100	20.0	40.0	60.0	80.0	100.0
110	18.2	36.4	54.5	72.8	91.0
120	16.7	33.4	50.0	66.7	83.3
130	15.4	30.8	46.1	61.5	76.9
140	14.3	28.6	42.8	57.2	71.5
150	13.3	26.6	40.0	53.3	66.7
160	12.5	25.0	37.5	50.0	62.5
170	11.8	23.6	35.3	47.0	58.8
180	11.1	22.2	33.3	44.4	55.5
190	10.5	21.0	31.6	42.1	52.6
200	10.0	20.0	30.0	40.0	50.0

The armature diameter, D, and the peripheral speed, S, may be obtained from the values of the pole pitch τ in the above table, by means of the simple relations:

$$D = \frac{\tau \times P}{\pi} \qquad S = \frac{\tau \times P \times R}{60}.$$

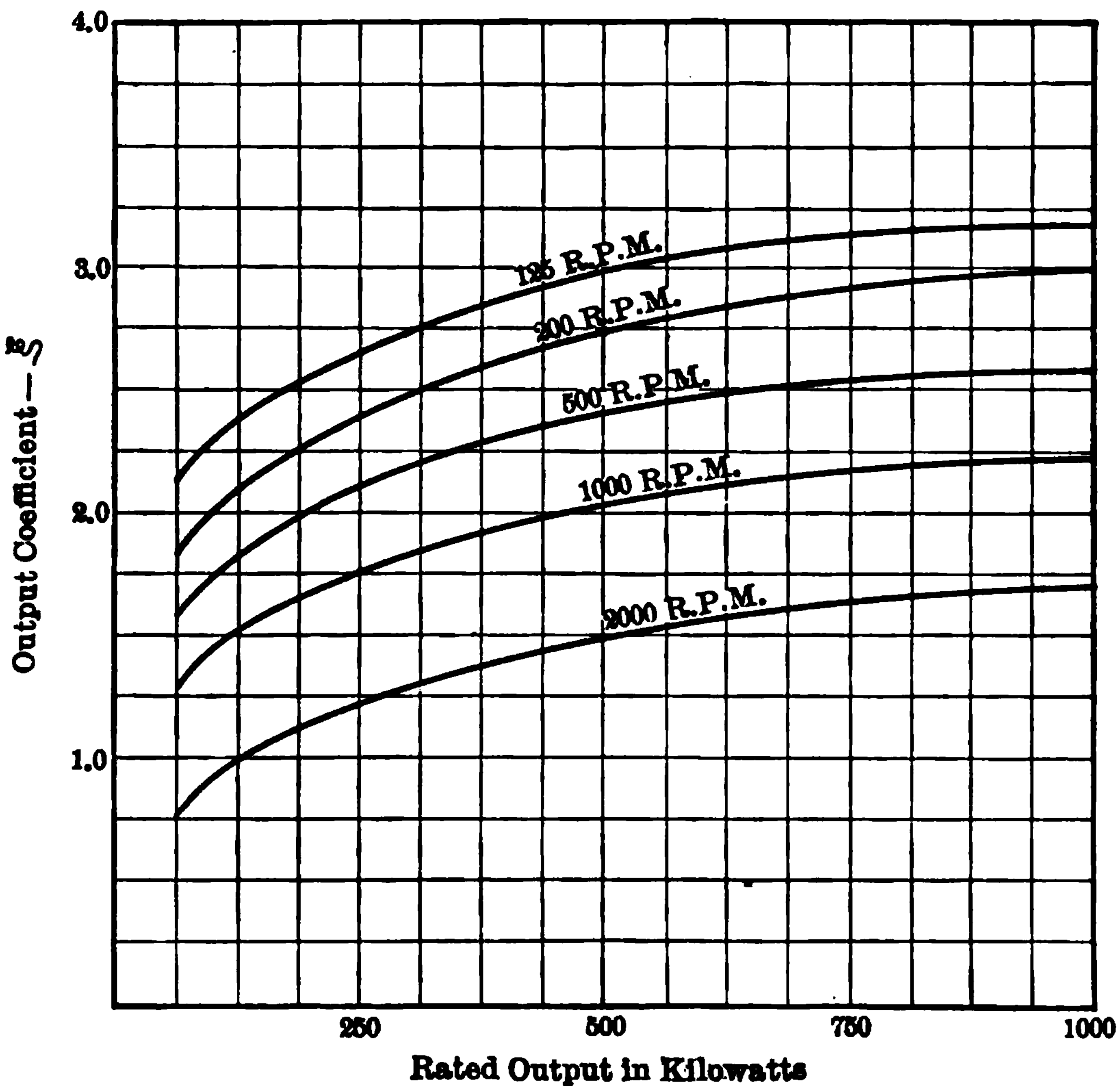


FIG. 240.— Output coefficients of continuous current dynamos.

The "Output Coefficient." *— The output coefficient is denoted by the letter ξ, and its value may be obtained from the formula

$$\xi = \frac{W}{D^2 \times \lambda_g \times R}$$

where W = rated output in watts,
D = air gap diameter of armature in decimetres,†
λ_g = Gross core length of armature in decimetres,†
R = Rated speed in revolutions per minute.

* The subject of the "Output Coefficient" for continuous current machines has already been treated in Chap. II, from one point of view.

† It is convenient in the output coefficient formula, to express D and λg in decimetres, and, in the hands of practitioners, this introduces no confusion with the practice of expressing D and λg in *centimetres* on all other occasions. But *students* must be careful not to make mistakes in this matter.

In Fig. 240 are shown curves for the "output coefficient" plotted as a function of the kilowatts output, and for speeds ranging from 125 to 2000 R.P.M. and for 500 volts.* From these curves it is seen that the output coefficient is higher for a slow speed machine than for a high speed design of the same rated output. This circumstance is partly attributable to the thermal and partly to the commutation difficulties encountered in extra high speed designs. In such cases, i.e., in extra high speed designs, the diameter has to be kept small because of the magnitude of the mechanical stresses arising from the high peripheral speeds, and consequently large centrifugal forces. Hence it is that in an extra high speed machine we always have a long armature when compared with that of a moderate speed machine for the same rated output. It follows that in extra high speed machines the embedded length of the short circuited turns is much greater than in moderate speed machines of the same output. As a consequence of this and of the high periodicity of commutation, the reactance voltage for a given current per conductor becomes large, thereby rendering necessary the introduction of interpoles to enable commutation to be performed satisfactorily. The space required by these interpoles necessitates an increase in diameter, or, if this is impossible owing to the limitations imposed by mechanical stresses, then the polar pitch, τ, must be increased by employing a smaller number of poles.

The output coefficient is also influenced to a certain extent by the voltage, for if the voltage is high, then more space must be provided for insulation, and there is thereby necessitated a lower output coefficient than for a low voltage machine. Again, from the curves in Fig. 240, we notice that the output coefficient is higher, the higher the output of the machine; this is to a considerable extent in consequence of the higher slot space factor which may be obtained in the case of large, as compared with small machines, thus permitting the use of a relatively smaller diameter.

From Fig. 240 we may obtain approximate preliminary assumptions for the output coefficients of machines for various rated outputs and various rated speeds.

Since $\xi = \dfrac{W}{D^2 \times \lambda_g \times R}$, we have $\lambda_g = \dfrac{W}{D^2 \times \xi \times R}$.

* The circumstances attending the preparation of any particular design, often justify wide departures from these output coefficient curves.

If, now, we assign values to D, W, and R, and from Fig. 240 obtain ξ for a machine corresponding to this data, we may, by substituting these values in the above equation, at once obtain a value for λ_g. As an example let us take the case of the 6-pole 1000 R.P.M. machines which we have been considering, and let us assign to D the value of 127 centimetres = 12.7 decimetres. This is the value corresponding to curve e, Fig. 238. Let us assign to W the value of 250,000 watts rated output. Then ξ obtained from Fig. 240 for this data is equal to 1.75.

Substituting these values in the above equation, we have

$$\lambda_g = \frac{250{,}000}{(12.7)^2 \times 1.75 \times 1000} = 0.89 \text{ decimetre.}$$

As an alternative design let us give to D the value 10.2 decimetres, i.e., that corresponding to curve d, Fig. 238, keeping the same values of W and R and consequently also of ξ as before.

Therefore, $$\lambda_g = \frac{250{,}000}{(10.2)^2 \times 1.75 \times 1000} = 1.38 \text{ decimetres.}$$

Continuing in this manner, the following values are obtained, for λ_g for the corresponding values of D given by curves a to e of Fig. 238, the values of D and λ_g being given in centimetres.

D.	λ_g.
127	8.9
102	13.8
76	24.8
51	55
25.4	220

Having deduced values for λ_g corresponding to the various diameters, as given in the preceding table, for an output of 250 kw. and for a speed of 1000 revolutions per minute, we can mark a point on each diameter curve of Fig. 238 corresponding to the value of λ_g obtained in the above table. For example, for $D = 102$ we have $\lambda_g = 13.8$, and marking the point $\lambda_g = 13.8$ on the curve for $D = 102$ in Fig. 238 we know that this point corresponds to an output of 250 kw. at 1000 R.P.M. We may also mark similar points on each of the diameter curves in Fig. 238, thus,

on the curve for $D = 76$, the point is $\lambda_g = 24.8$,

and on the curve for $D = 127$, $\lambda_g = 8.9$.

A curve drawn through the three points thus obtained gives an output line for 250 kw. at 1000 R.P.M. This has been done in Fig. 241, giving us a set of curves showing the reactance voltages as a function of the gross core length λ_g, the diameter D, and also of the output.

The curves in Fig. 241 all relate to designs for 1000 R.P.M. In Fig. 242 are plotted in curves the results of similar investigations for 6-pole designs for other rated speeds. Attention should be drawn to the very small values of the reactance voltage at the low rated speed of 125 R.P.M. as compared with the very high values at the

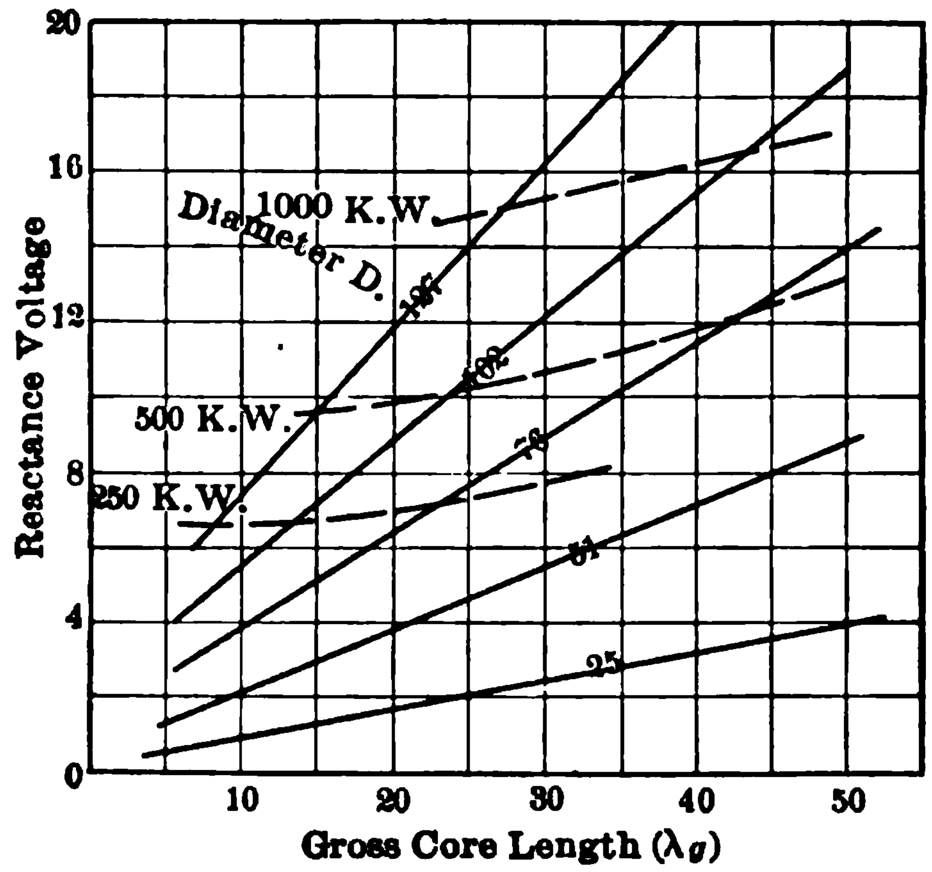


Fig. 241.— Reactance voltage curves for 6-pole 1000 r.p.m. designs for various outputs and diameters.

high rated speed of 2000 R.P.M. At the former speed (125 R.P.M.) it is rarely necessary or desirable to employ interpoles even for the larger values of λ_g.* At the latter speed (2000 R.P.M.), interpoles are necessary in all machines except those of exceedingly small rated output.

In Fig. 243 are plotted curves showing for 6-pole designs in which $\lambda_g = 30$, the relation between the reactance voltage and the rated speed in R.P.M. for machines of 125, 250, 500, and 1000 kw. rated output. These curves show that the reactance voltage increases both with rated output and speed. In Fig. 244 are plotted curves showing

* Very low speed machines of large rated output and for a pressure of some 1000 volts or more, often work out best with more than one turn per segment and with interpoles.

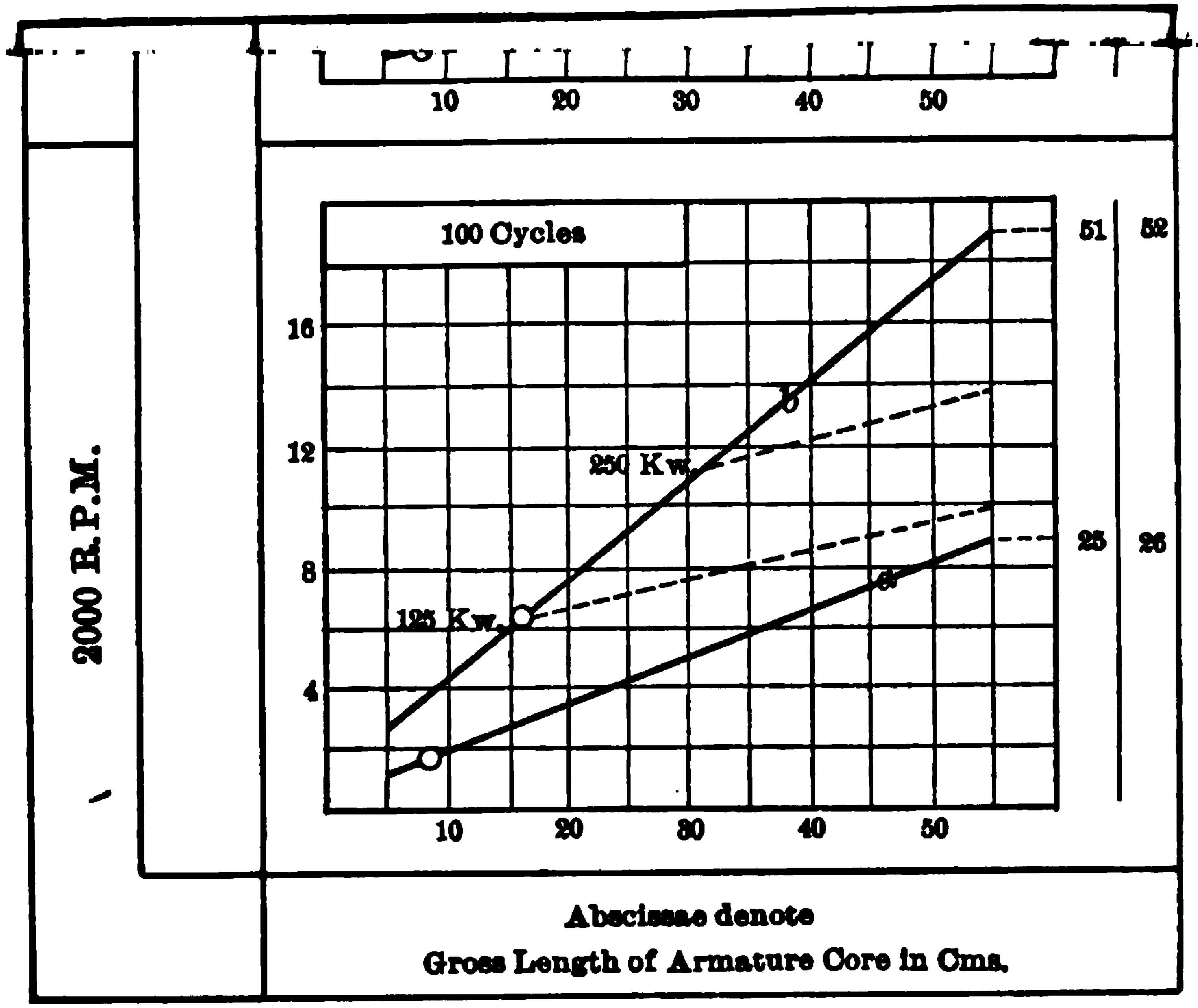


FIG. 242.— Design chart for determining preliminary dimensions and reactance voltages for 6-pole continuous current dynamos at various rated speeds and outputs.

the relation between the reactance voltage and the rated output for speeds of 125, 250, 500, 1000, and 2000 R.P.M., for these 6-pole designs with $\lambda_g = 30$. This set of curves is derived from the set shown in Fig. 243.

In Fig. 245 are corresponding groups of curves for designs with 4, 6, 8, 12, and 16 poles, and all for a speed of 1000 R.P.M. These curves are useful in comparing the relative merits of different numbers of poles for a 1000 R.P.M. design of a given rated capacity.

In Fig. 246 are plotted three sets of curves, which show the relation between the reactance voltage and the rated output in kilowatts

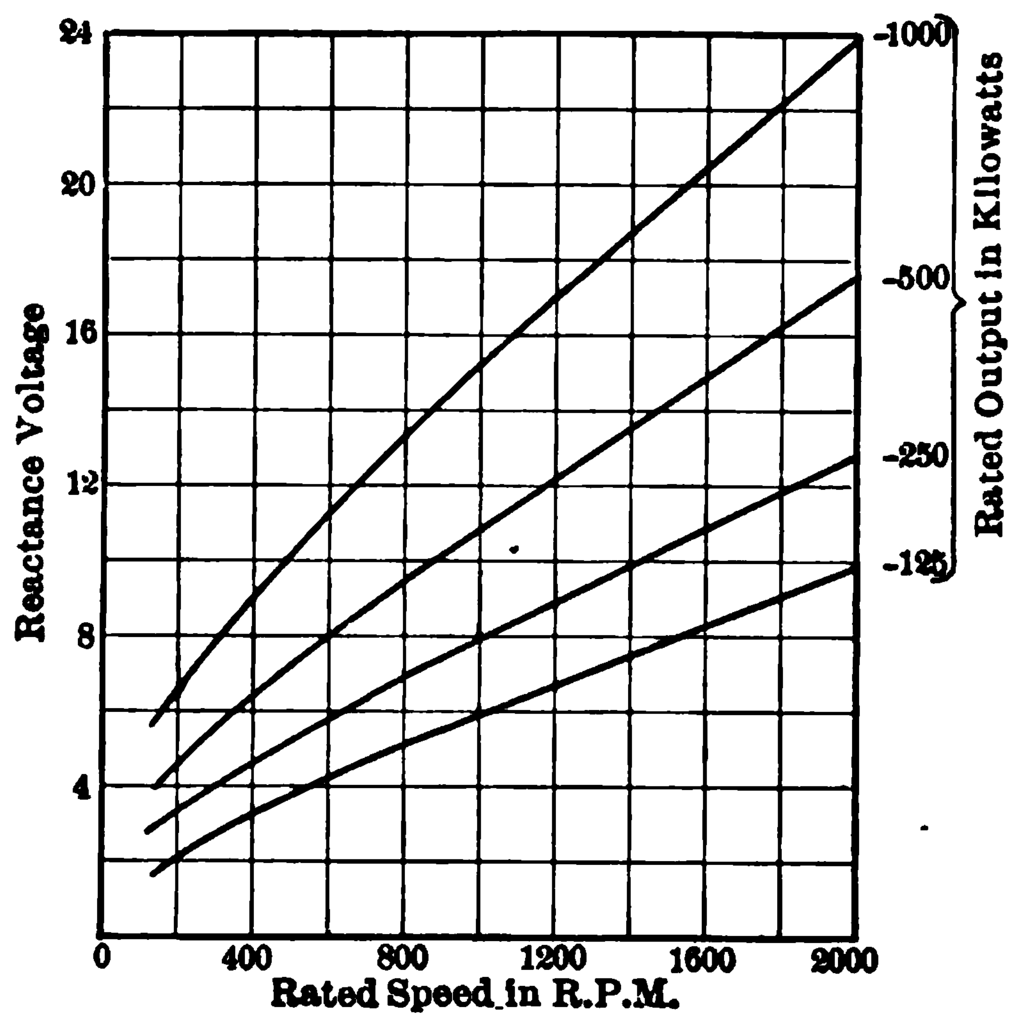


FIG. 243. — Reactance voltage curves for 6-pole designs for $\lambda g = 30$.

for various numbers of poles, for three values of λ_g (20, 30, and 40 centimetres) and for a rated speed of 1000 R.P.M. These curves show that the higher the number of poles, the lower the reactance voltage. Furthermore, the greater the value of λ_g, the higher the reactance voltage. If, for a given rated output and speed, λ_g is increased, the value of D, the diameter must be decreased, in order to keep the value of $D^2\lambda_g$ the same as before, because the output coefficient is dependent chiefly on the rated output and rated speed, as shown in Fig. 240. Consequently, generally speaking, the larger the diameter, the lower the reactance voltage for a given output coefficient. This increase in the diameter should, however, not be carried too far,

as the relatively slight reduction in the reactance voltage does not warrant such procedure, because of the higher cost of manufacture inherent to designs of large diameter and because of mechanical limitations relating to the peripheral speed, shape of magnet cores, etc. Where the reactance voltage has exceeded satisfactory limits interpoles must be introduced.

The large chart in Fig. 247 comprises groups of curves for various speeds and numbers of poles. The five vertical columns relate respectively to 4, 6, 8, 12, and 16-pole designs, and the five horizontal

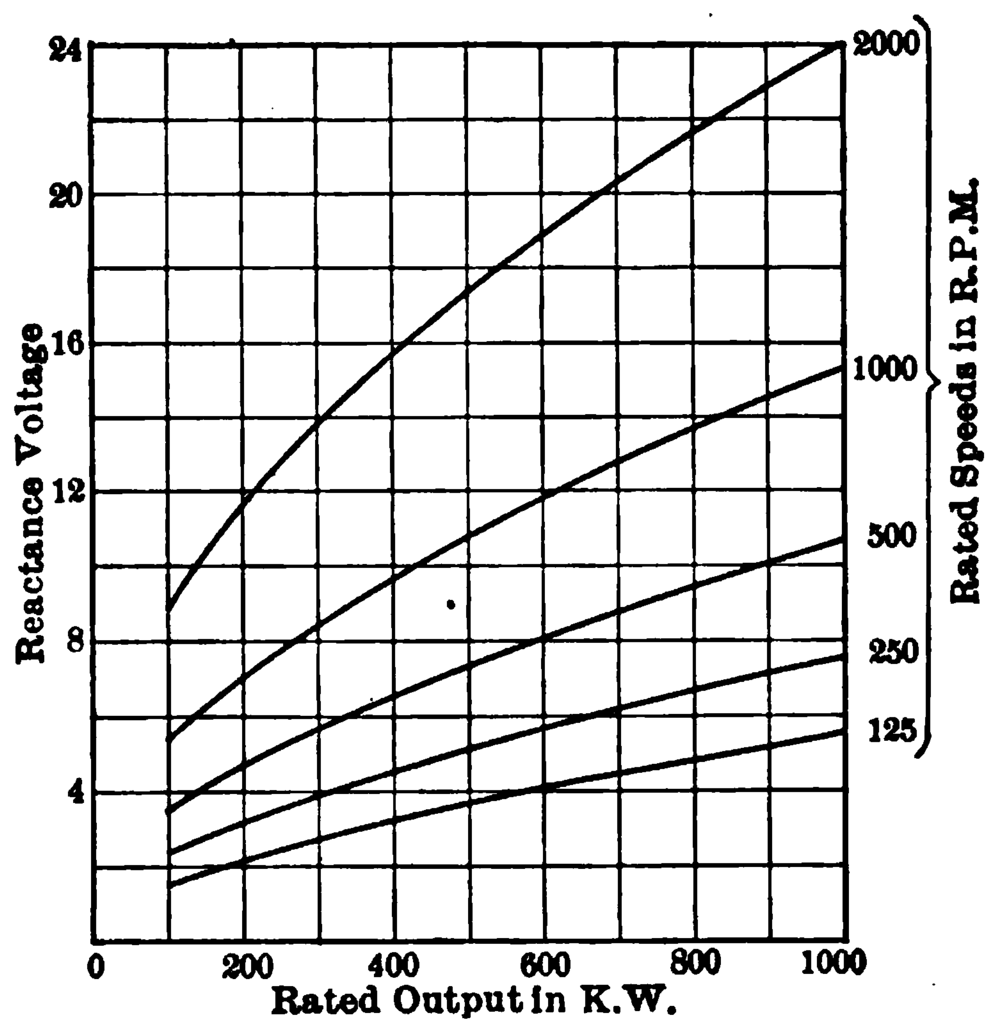


FIG. 244.— Reactance voltage curves for 6-pole designs for $\lambda g = 30$.

rows, to rated speeds of 125, 250, 500, 1000, and 2000 R.P.M. In this chart, all the curves have been plotted to the same scale in order that one may obtain a true conception of the influence of the various factors. In the case of the low speed designs, however, and more especially of those with relatively few poles, some of the individual groups of curves are thereby rendered less clear. These particular groups of curves have consequently been again plotted and to more suitable scales, in Figs. 248, 249, and 250.

In Figs. 245 to 250 the large circles indicated on most of the curves, constitute loci of designs of such proportions that magnet cores of circular cross section are most appropriate. Of course a circular cross

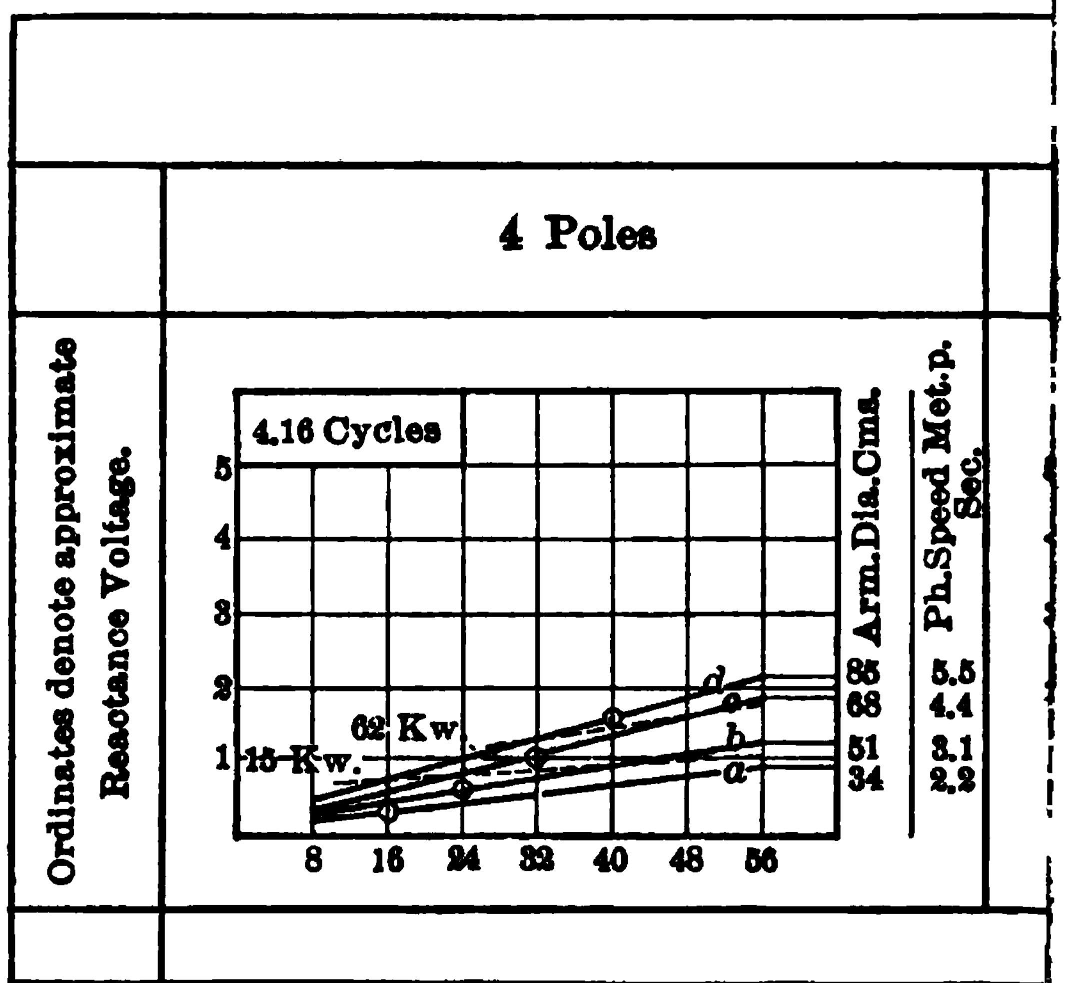


FIG. 248.— Design char

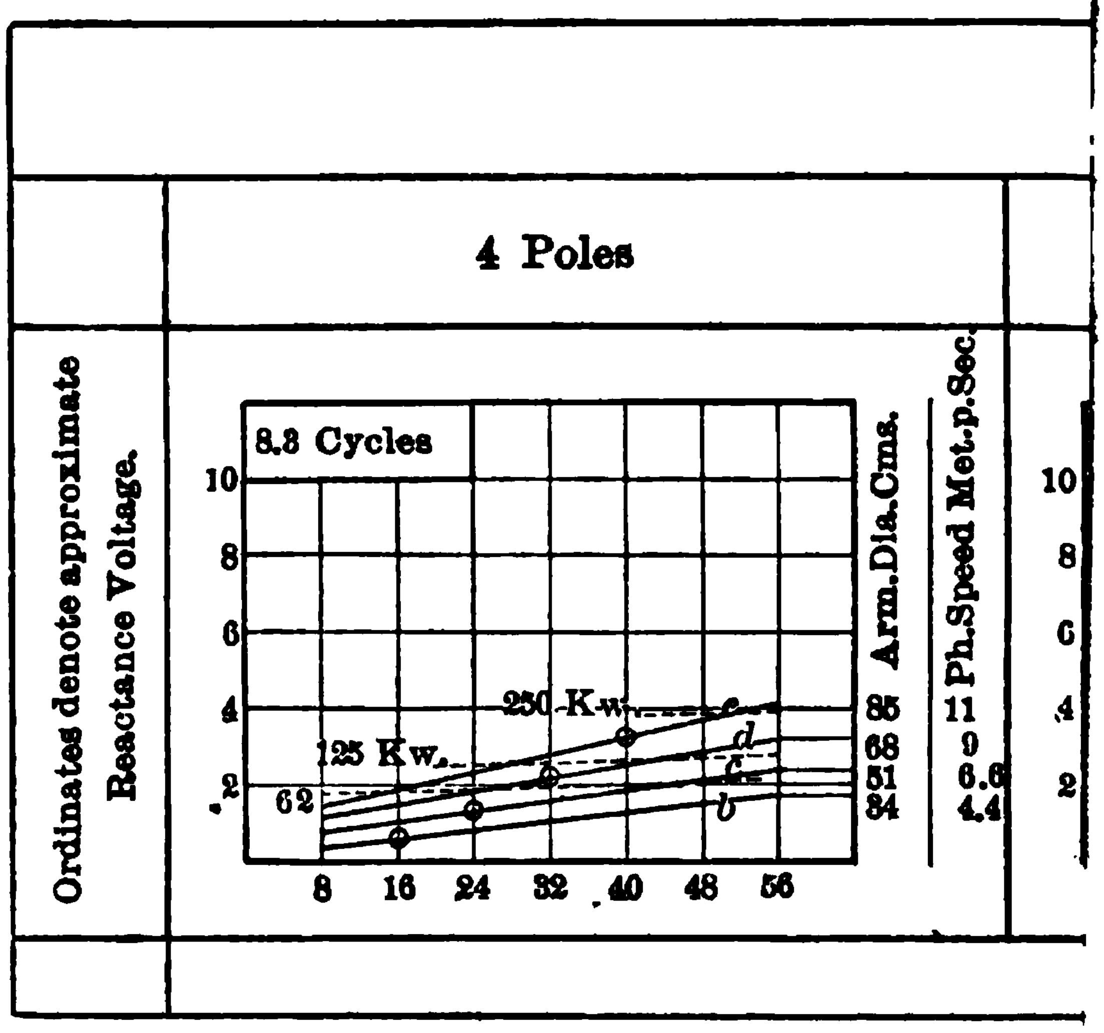


FIG. 249.— Design chart

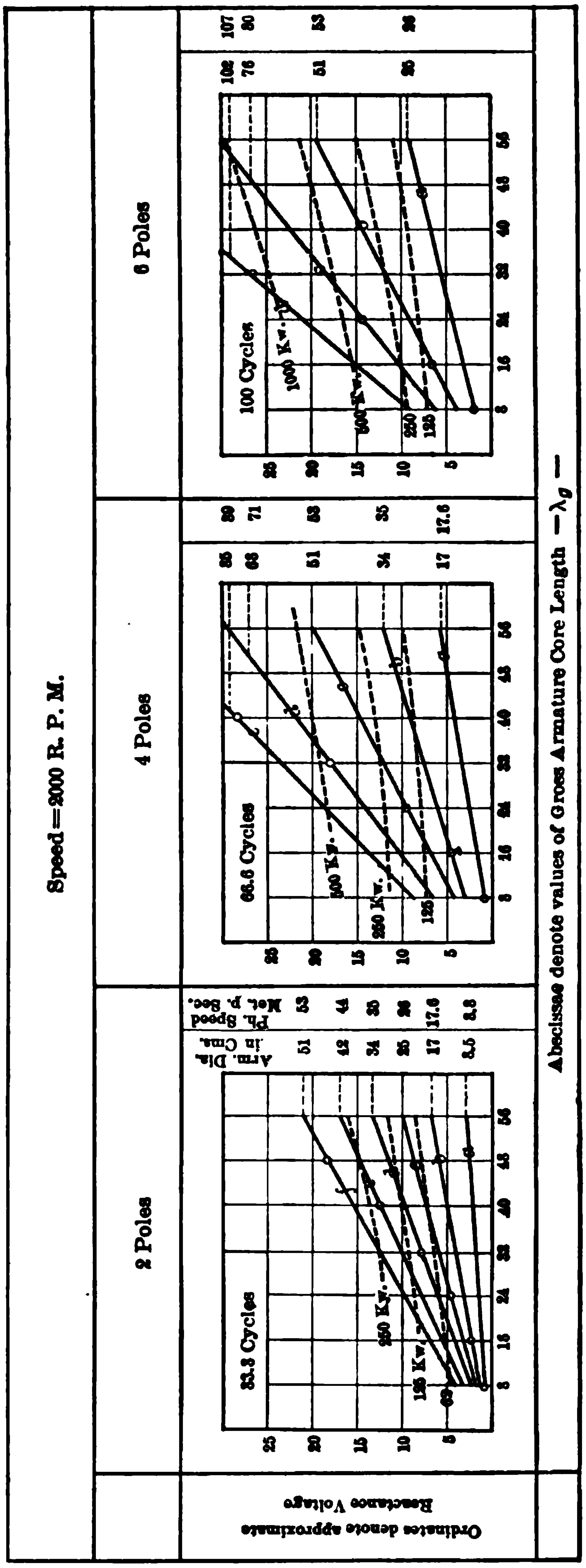


FIG. 250. — Design chart for preliminary dimensions and reactance voltage of 1000 r.p.m. continuous current dynamos with various numbers of poles.

section is generally practicable for designs lying anywhere in the neighbourhood of the track of these circles.

By a judicious use of the large chart in Fig. 247, and of these supplementary charts, the designer derives considerable assistance at the preliminary stages of the roughing out of the design.

The charts have been introduced at this point in order that reference may be made to them at subsequent stages of our investigations.

Against each of the full line curves is set not only the speed in revolutions per minute, but also the peripheral speed in metres per second, and no curves are drawn in which the peripheral speed exceeds 100 metres per second. As a matter of fact, 80 metres per second is a very high limit, and higher speeds are shown only out of consideration

for the rapid strides toward greater permissible speeds, which have recently been made and are still in progress.

It is proposed now to describe a method of procedure suitable for determining the leading dimensions of a design for a given rated output, voltage, and speed.

Let us first turn our attention to a design for

500 kw.
250 volts
125 R.P.M.

Denoting by I the amperes output at rated load, we have

$$I = \frac{500,000}{250} = 2000.$$

Letting P = number of poles, we obtain for $\frac{I}{P}$ the amperes per circuit, the following values:

P.	$\frac{I}{P}$.
8	250
12	167
16	125

In this case we certainly should not find it desirable to employ less than eight poles, since even with eight poles we have $\frac{I}{P} = 250$, a rather high value. For high speeds it becomes inevitably necessary to employ still higher values for the amperes per pole; but for low speeds, where we have more freedom for choice, 200 amperes per pole is not willingly exceeded. From the 8-pole group of curves in Fig. 248, we find that the 500-kw. line cuts curve e (10,000 armature ampere turns per pole) at $\lambda_g = 45$. The experienced designer knows that this is an undesirably high armature strength for a machine of so small a capacity as 500 kw. But let us nevertheless take the design into consideration, together with the 12 and 16-pole designs which have more satisfactory properties.

Knowing the amperes per circuit $\frac{I}{P}$, we obtain T, the number of turns per pole (which is also the number of turns in series between

brushes), by dividing the armature strength in ampere turns per pole by $\frac{I}{P}$. With one turn per segment, which is the case in most designs of any size, T is also equal to the number of commutator segments per pole.

With these explanations, we are ready, by means of the curves in Fig. 248, to compile the schedule in Table 48 for the purposes of a preliminary survey of the characteristics of various alternative designs. From amongst these we must endeavour to select the design embodying a maximum of desirable, and a minimum of undesirable, properties.

TABLE 48.

ALTERNATIVE TRIAL DESIGNS FOR A RATED OUTPUT OF 500-KW. 125 R.P.M. 250 VOLTS.

		Armature Ampere Turns per Pole.					
		5000	6000	7000	8000	9000	10,000
8 Poles, $\frac{I}{P} = 250$, $N = 8.3$.	Armature diameter (D)	...	...	...	...	...	170
	Gross length (λ_g)	...	...	...	...	...	45
	Pole pitch (τ)	...	...	...	...	...	67
	$D \times (\lambda_g + 0.7\ \tau)$	...	...	...	...	...	15,650
	Reactance voltage	...	...	...	...	...	3.5
	Arm. periph. speed (Sa)	...	...	...	...	...	11
	Com. periph. speed (Sc)	...	...	...	...	...	3.33
	Armature turns per pole (**T**)	...	...	...	...	...	40
	$\frac{\lambda_g}{\tau}$	...	...	...	...	...	0.67
12 Poles, $\frac{I}{P} = 167$, $N = 12.5$.	Armature diameter (D)	...	153	178	204	...	...
	Gross length (λ_g)	...	56	40	32	...	...
	Pole pitch (τ)	...	40	47	53	...	...
	$D \times (\lambda_g + 0.7\ \tau)$	...	12,850	13,000	14,100	...	...
	Reactance voltage	...	4.0	3.4	4.0	...	...
	Arm. periph. speed (Sa)	...	10	11.6	13.3	...	...
	Com. periph. speed (Sc)	...	4.5	5.25	6	...	...
	Armature turns per pole (**T**)	...	36	42	48	...	...
	$\frac{\lambda_g}{\tau}$	...	1.4	0.85	0.60	...	...
16 Poles, $\frac{I}{P} = 125$, $N = 16.7$.	Armature diameter (D)	...	204	238	272	...	...
	Gross length (λ_g)	...	32	24	18	...	...
	Pole pitch. (τ)	...	40	47	53	...	...
	$D \times (\lambda_g + 0.7\ \tau)$	...	12,240	13,560	14,950	...	...
	Reactance voltage	...	3.2	2.7	2.4	...	...
	Arm. periph. speed (Sa)	...	13.3	15.3	17.5	...	...
	Com. periph. speed (Sc)	...	8	9.3	10.7	...	...
	Armature turns per pole (**T**)	...	48	56	64	...	...
	$\frac{\lambda_g}{\tau}$	...	0.80	0.51	0.34	...	...

NOTE. — The peripheral speeds are in metres per second.

Other things being equal, it is desirable to have a machine with magnet cores of circular cross section, a low reactance voltage,* a low peripheral speed of commutator, and a low Total Works Cost. The low peripheral speed of commutator must not be purchased at the expense of undesirably thin commutator segments. There appears, however, no sufficient reason for objecting, even in large machines, to segments with a thickness including insulation, of not less than 5 millimetres at the surface; and, in small machines, segments of considerably less thickness are widely employed, and with advantage.

For preliminary purposes, the commutator peripheral speed is estimated on the assumption that the width of segment, plus insulation, at the periphery of the commutator, is equal to 5 millimetres. Where this thickness leads to a needlessly low peripheral speed, the commutator may, at a later stage of the calculations, be increased in diameter and decreased in length.

For more precise determinations of the reactance voltage we must apply a correction as already briefly explained on page 337. Thus, let us take the case of the 12-pole 6000 ampere turn design of Table 48. The reactance voltage as given on the chart for $D = 153$, $\lambda_g = 56$, and a rated output of 500 kw. is 4.0 volts. But this is on the assumption of a design with 150 armature ampere turns per centimetre of gap periphery, whereas from the curve in Fig. 240, some 143 armature ampere turns per centimetre of gap periphery is given as representative for 500 kw. rated output. For a design in which we retain the above values of D and λ_g (i.e., 153 and 56), and use this representative value of 143 for the armature ampere turns per centimetre of gap periphery, the armature strength per pole is in reality only

$$\frac{143}{150} \times 6000 = 5700,$$

and the reactance voltage is only

$$\frac{143}{150} \times 4.0 = 3.8.$$

* This is not such an important feature of an interpole machine as it is in the case of a machine without interpoles, except in cases of large rated outputs at extra high speeds where the reactance voltage must in any case be very high and where interpoles cannot be relied upon for ensuring satisfactory results.

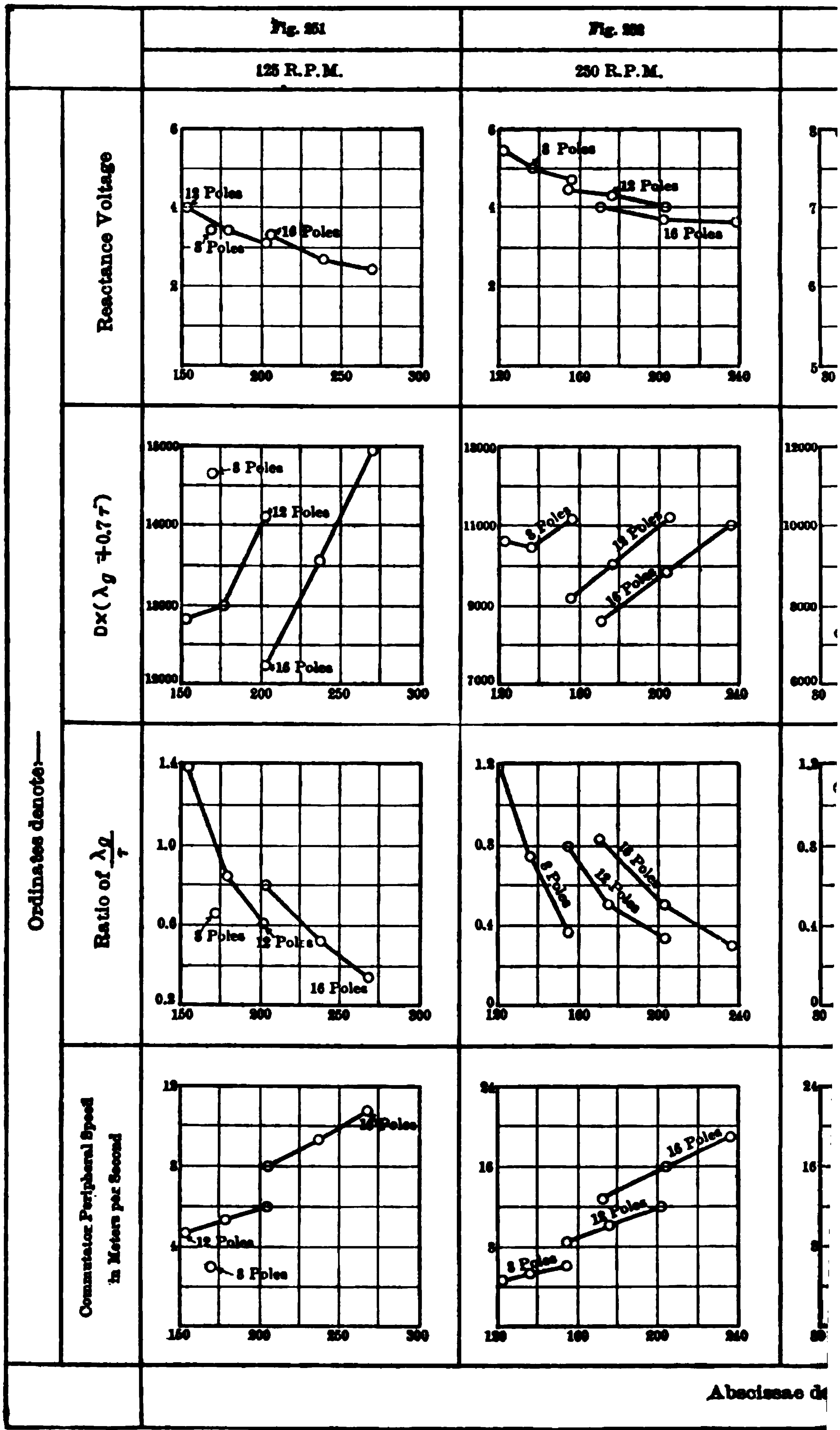
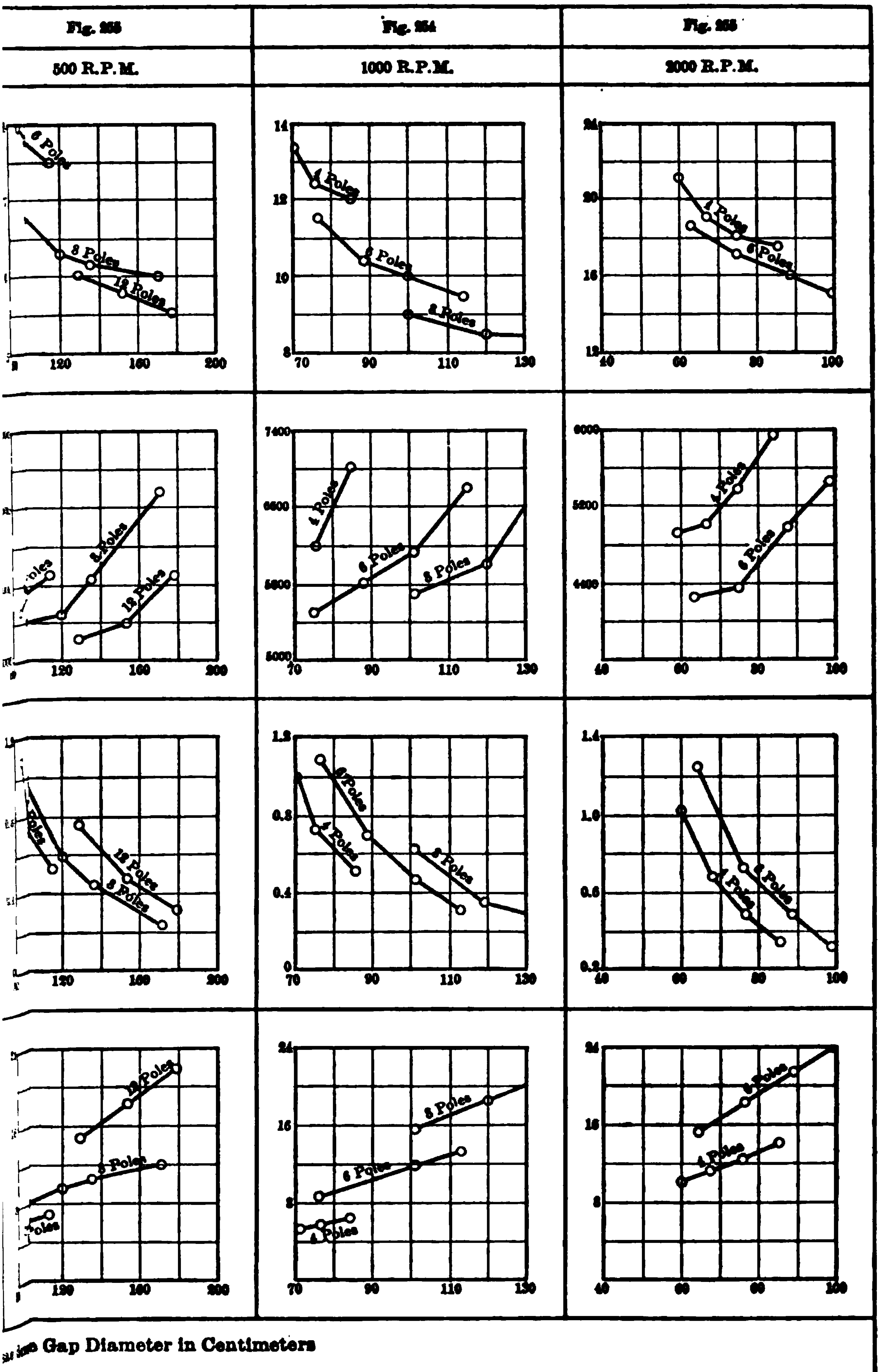


FIG. 251–255.—Curves relating to preliminary designs fo
plotted from Sche



...0 kw. 250 volt continuous current machines for various speeds
...s in Tables 48 to 53.

Of course in this instance it would be an utterly misplaced and futile effort at exactness to apply the correction, but in cases where we are dealing with designs for much smaller rated outputs, — take, for instance, a machine for some 50 kw. rated output, — we see from Fig. 239 that the value of the armature ampere turns per centimetre of gap periphery is more of the order of 115, and in this case the application of the correction to the armature strength in ampere turns per pole and to the reactance voltage, is essential. For the rating taken to illustrate this point, i.e., for a design for a rated output of some 50 kw., the reactance voltage is only some $\frac{115}{150}$; i.e., some 77 per cent of the value read from the charts. Obviously, the application of this correction is an exceedingly simple matter, and should be kept in mind for ratings materially below those with which this treatise is chiefly concerned.

The precise estimation of the Total Works Cost is a matter to be treated with considerable care, but for purposes of rough preliminary comparisons, it is sufficient to take the Total Works Cost as proportional to $D \times (\lambda_g + 0.7\,\tau)$,

where D = diameter at air gap in centimetres,
λ_g = gross core length in centimetres,
τ = polar pitch in centimetres.

This quantity, $D \times (\lambda_g + 0.7\,\tau)$, if multiplied by a factor K, which varies for different types and sizes of machine, gives a fair approximation to the Total Works Cost. For the purposes of a preliminary study, however, we may neglect K and compare the Total Works Cost as expressed in terms of

$$D \times (\lambda_g + 0.7\,\tau).$$

At the time of the preparation of a schedule such as that in Table 48, the endeavour should be made to avoid entering up inevitably undesirable designs. The remaining instances, i.e., those which appear likely to come within the range of possible usefulness, are subsequently compared, and a convenient means of carrying out this comparison consists in plotting such a series of curves as those given in Fig. 251.

From these curves we see that the Total Works Cost is the least with the greater number of poles, with a moderate diameter and with low armature strength. The 8-pole design is inferior both as regards

Total Works Cost and commutation, and is withdrawn from further comparison. The commutator peripheral speed is very low for all the designs, being 11 metres per second for the largest diameter. Taking this into consideration, we should at a later stage of the calculation, substitute a thicker commutator segment and employ a shorter commutator of larger diameter.

The 12-pole designs as compared with the 16-pole designs have somewhat higher reactance voltage, but there is very little choice between them as regards Total Works Cost, as may be seen from Table 48. On the whole, the comparison is slightly in favour of the 16-pole designs. The preferable design will have a diameter of some 230 centimetres and a ratio of $\frac{\lambda_g}{\tau}$ of 0.60. This ratio permits of employing a circular diameter of magnet core.

In the course of working out this design more in detail, it may prove desirable to depart considerably from this outline — indeed, it is usually best to work out several alternative designs. It is intended here merely to indicate a method of arriving at a reasonably sound starting-point.

A 12-pole design with an armature diameter of 200 centimetres, is also well within the range of being practicable, for, although the reactance voltage is a trifle higher, the reduced commutator peripheral speed is so low on the reference basis of 5-millimetre segments, that the final design of commutator can have a considerably increased diameter and decreased length. Some of the leading data of these 12 and 16-pole designs are given in Table 49.

TABLE 49.

LEADING DATA OF 12 AND 16 POLE DESIGNS FOR 500 KW. 125 R.P.M. CONTINUOUS CURRENT GENERATOR.

Number of poles	12	16
D	200	230
λ_g	35	27
T.W.C.	14,000	13,300
Armature a't's per pole	8,100	6,700
Reactance voltage	3.0	2.8
Commutator peripheral speed Sc (5 mm. segments)	6	9

It would be neither necessary nor desirable to employ interpoles in either of these designs.

It is thus clearly indicated that 12, 14, or 16 poles and some 6500 to 8500 armature ampere turns per pole in any one of these three cases, and a gap diameter of somewhere between 200 centimetres for the 12-pole, and 230 centimetres for the 16-pole design, are the general order of magnitudes which should be employed as the basis for these trial designs.

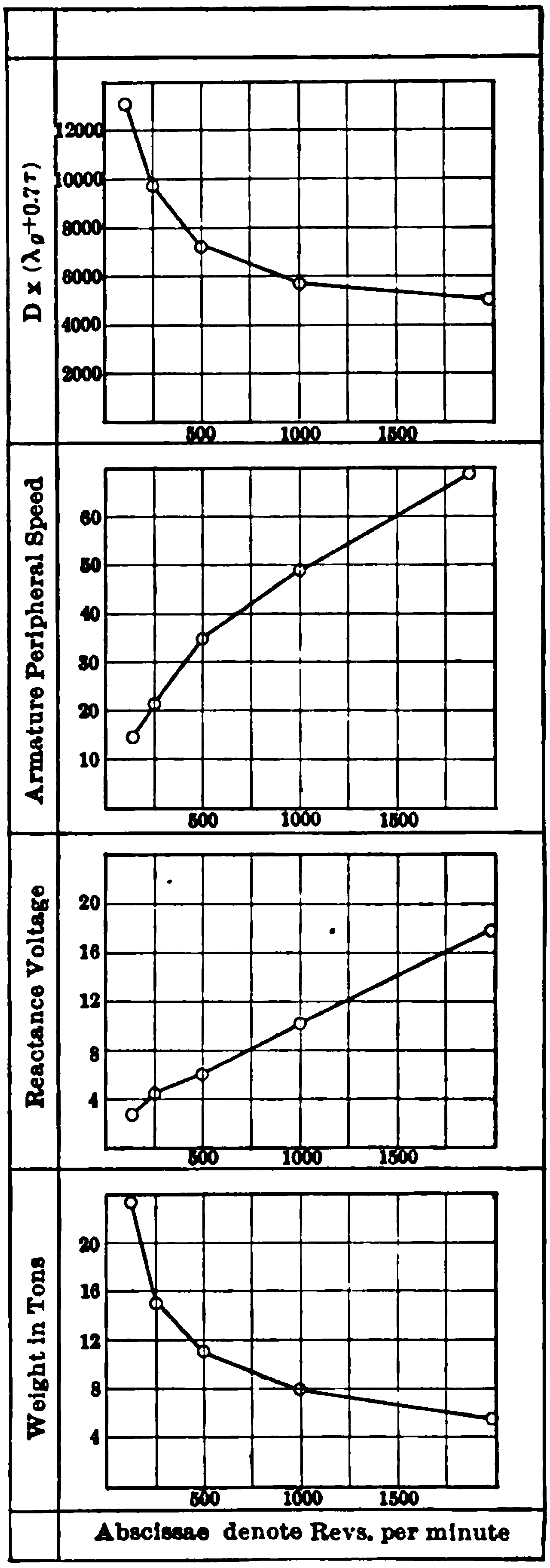


Fig. 256. — Data of 500-kw. 250-volt designs at different rated speeds selected from curves in Figs. 251-255.

It is often desirable to take two or more of the best designs with a given number of poles and also for *different* numbers of poles, and work them out more in detail before determining upon the preferable design. However, for an extended comparison of a great number of different ratings, it is often impracticable on account of the limited available time, to thoroughly work out a number of alternative designs. Under such circumstances, it would appear reasonable in this instance to select the 16-pole design.

In a similar manner we have prepared schedules for 500-kw. 250-volt designs of 125 (the above design), 250, 500, 1000, and 2000 R.P.M. and corresponding sets of curves. These schedules and curves are set forth in Tables 48 to 53 and Figs. 251 to 255. From a perusal of these schedules and of Figs. 251 to 255 we may prepare Table 54 showing the leading data of the preferable design

for each speed. In Fig. 256 are plotted, in terms of the speed in revolutions per minute, the leading constants for the 500-kw. 250-volt designs. The total weights plotted in the lowest curve of Fig. 256 have been derived by means of the data given in the curves of Fig. 17, Chapter II.

TABLE 50.

ALTERNATIVE TRIAL DESIGNS FOR A RATED OUTPUT OF 500 KW. 250 R.P.M. 250 VOLTS.

		Armature Ampere Turns per Pole.					
		5000.	6000.	7000.	8000.	9000.	10,000.
8 Poles, $\frac{I}{p}$ = 250, N = 16.7.	Armature diameter (D)	...	...	120	136	153	...
	Gross length (λ_g)	...	...	56	40	32	...
	Pole pitch (τ)	...	...	47	53	59	...
	$D\times(\lambda_g+0.7\,\tau)$	...	...	10,600	10,500	11,200	...
	Reactance voltage	...	...	5.5	5.0	4.8	...
	Arm. periph. speed (Sa)	...	...	15	17	19.5	...
	Com. periph. speed (Sc)	...	...	4.7	5.4	6.0	...
	Armature turns per pole (T)	...	...	28	32	36	...
	$\frac{\lambda_g}{\tau}$	...	...	1.2	0.75	0.55	...
12 Poles, $\frac{I}{p}$ = 167, N = 25.	Armature diameter (D)	...	153	178	204	...	...
	Gross length (λ_g)	...	32	24	18	...	...
	Pole pitch (τ)	...	40	47	53	...	...
	$D\times(\lambda_g+0.7\,\tau)$	...	9200	10,000	11,200	...	...
	Reactance voltage	...	4.6	4.3	4.0	...	...
	Arm. periph. speed (Sa)	...	20	23	26	...	...
	Com. periph. speed (Sc)	...	9	10.6	12.0	...	...
	Armature turns per pole (T)	...	36	42	48	...	...
	$\frac{\lambda_g}{\tau}$	...	0.8	0.51	0.34	...	...
16 Poles, $\frac{I}{p}$ = 125, N = 33.	Armature diameter (D)	170	204	238	...	...	...
	Gross length (λ_g)	28	20	14	...	...	...
	Pole pitch (τ)	33	39	46	...	...	...
	$D\times(\lambda_g+0.7\,\tau)$	8600	9800	11,000	...	...	...
	Reactance voltage	4	3.8	3.7	...	...	...
	Arm. periph. speed (Sa)	21.5	26	31	...	...	...
	Com. periph. speed (Sc)	13.5	16	19	...	...	...
	Armature turns per pole (T)	40	48	56	...	...	...
	$\frac{\lambda_g}{\tau}$	0.85	0.51	0.31	...	...	...

NOTE.—All peripheral speeds are in metres per second.

TABLE 51.

ALTERNATIVE TRIAL DESIGNS FOR A RATED OUTPUT OF 500 KW. 500 R.P.M. 250 VOLTS.

		Armature Ampere Turns per Pole.					
		5000.	6000.	7000.	8000.	9000.	10,000.
6 Poles, $\frac{I}{p} = 334$, $N = 25$.	Armature diameter (D)	...	...	89	102	114	...
	Gross length (λ_g)	...	...	50	40	32	...
	Pole pitch (τ)	...	...	46	53	59	...
	$D\times(\lambda_g+0.7\,\tau)$	...	...	7300	7800	8300	...
	Reactance voltage	...	...	8.0	7.8	7.5	...
	Arm. periph. speed (Sa)	...	...	23	26	30	...
	Com. periph. speed (Sc)	...	...	5.2	6.0	6.8	...
	Armature turns per pole (T)	...	...	21	24	27	...
	$\frac{\lambda_g}{\tau}$	...	...	1.10	0.75	0.54	...
8 Poles, $\frac{I}{p} = 250$, $N = 34$.	Armature diameter (D)	...	102	120	136	170	...
	Gross length (λ_g)	...	40	27	23	15	...
	Pole pitch (τ)	...	40	47	53	66	...
	$D\times(\lambda_g+0.7\,\tau)$	...	6960	7200	8160	10,400	...
	Reactance voltage	...	6.8	6.3	6.2	6.0	...
	Arm. periph. speed (Sa)	...	27	31	35	44	...
	Com. periph. speed (Sc)	...	8	9.5	10.5	12	...
	Armature turns per pole (T)	...	24	28	32	36	...
	$\frac{\lambda_g}{\tau}$	...	1.00	0.58	0.44	0.23	...
12 Poles, $\frac{I}{p} = 167$, $N = 50$.	Armature diameter (D)	127	153	179	...	...	...
	Gross length (λ_g)	25	18	14	...	...	...
	Pole pitch (τ)	33	39	46	...	...	...
	$D\times(\lambda_g+0.7\,\tau)$	6100	6900	8200	...	...	...
	Reactance voltage	6.0	5.8	5.5	...	...	...
	Arm. periph. speed (Sa)	33	40	47	...	...	...
	Com. periph. speed (Sc)	15	18	22	...	...	...
	Armature turns per pole (T)	30	36	42	...	...	...
	$\frac{\lambda_g}{\tau}$	0.76	0.46	0.31	...	...	...

NOTE.—The peripheral speeds are in metres per second.

TABLE 52.

Alternative Trial Designs for a Rated Output of 500 Kw. 1000 R.P.M. 250 Volts.

		Armature Ampere Turns per Pole.					
		5000.	6000.	7000.	8000.	9000.	10,000.
4 Poles, $\frac{I}{P} = 500$, $N = 33.4$.	Armature diameter (D)	...	...	...	68	75	85
	Gross length (λ_g)	...	...	...	54	42	35
	Pole pitch (τ)	...	...	...	53	58	67
	$D\times(\lambda_g+0.7\,\tau)$	...	...	...	6200	6200	7000
	Reactance voltage	...	...	...	13.5	12.5	12
	Arm. periph. speed (Sa)	...	...	...	35	39.5	44
	Com. periph. speed (Sc)	...	...	...	5.3	6.0	6.7
	Armature turns per pole (T)	...	...	...	16	18	20
	$\frac{\lambda_g}{\tau}$	...	...	...	1.00	0.73	.52
6 Poles, $\frac{I}{P} = 334$, $N = 50$.	Armature diameter (D)	...	76	88	102	114	...
	Gross length (λ_g)	...	44	33	24	19	...
	Pole pitch (τ)	...	38	46	52	59	...
	$D\times(\lambda_g+0.7\,\tau)$	...	5300	5800	6100	6800	...
	Reactance voltage	...	11.5	10.5	10.0	9.5	...
	Arm. periph. speed (Sa)	...	40	46	52	60	...
	Com. periph. speed (Sc)	...	9.0	10.5	12.0	13.5	...
	Armature turns per pole (T)	...	18	21	24	27	...
	$\frac{\lambda_g}{\tau}$	...	1.15	0.72	0.46	0.32	...
8 Poles, $\frac{I}{P} = 250$, $N = 66.7$.	Armature diameter (D)	...	102	120	136	...	...
	Gross length (λ_g)	...	25	18	14	...	...
	Pole pitch (τ)	...	40	47	53	...	...
	$D\times(\lambda_g+0.7\,\tau)$	...	5500	6100	6900	...	...
	Reactance voltage	...	9.0	8.5	8.3	...	...
	Arm. periph. speed (Sa)	...	53	62	71	...	...
	Com. periph. speed ((Sc)	...	16	19	21	...	...
	Armature turns per pole (T)	...	24	28	32	...	...
	$\frac{\lambda_g}{\tau}$	...	0.61	0.38	0.26	...	...

Note. — The peripheral speeds are in metres per second.

TABLE 53.

ALTERNATIVE TRIAL DESIGNS FOR A RATED OUTPUT OF 500 KW. 2000 R.P.M. 250 VOLTS.

		Armature Ampere Turns per Pole.					
		5000.	6000.	7000.	8000.	9000.	10,000.
4 Poles, $\frac{I}{p}$ = 500, N = 66.4.	Armature diameter (D)	...	...	60	68	76	85
	Gross length (λ_g)	...	...	48	36	28	23
	Pole pitch (τ)	...	...	47	53	60	67
	$D\times(\lambda_g+0.7\,\tau)$	...	...	4860	4960	5320	5950
	Reactance voltage	...	...	21	19	18	18
	Arm. periph. speed (Sa)	...	...	63	71	80	89
	Com. periph. speed (Sc)	...	...	9.3	10.7	12.0	13.4
	Armature turns per pole (T)	...	...	14	16	18	20
	$\frac{\lambda_g}{\tau}$	...	...	1.00	0.68	0.47	0.34
6 Poles, $\frac{I}{p}$ = 334, N = 100.	Armature diameter (D)	64	76	89	102	...	...
	Gross length (λ_g)	42	29	22	16	...	...
	Pole pitch(τ)	34	40	47	53	...	...
	$D\times(\lambda_g+0.7\,\tau)$	4225	4330	4900	5410	...	...
	Reactance voltage	19	17	16	15	...	...
	Arm. periph. speed (Sa)	67	80	93	107	...	...
	Com. periph. speed (Sc)	15	18	21	24	...	...
	Armature turns per pole (T)	15	18	21	24	...	...
	$\frac{\lambda_g}{\tau}$	1.24	0.725	0.47	0.30	...	...

NOTE. — The peripheral speeds are in metres per second.

TABLE 54.

500-KW. 250-VOLT CONTINUOUS CURRENT MACHINES FOR VARIOUS SPEEDS, SELECTED DIMENSIONS FROM FIGS 251-255.

(All dimensions in Centimetres).

Speed in Revolutions per Minute.	125.	250.	500.	1000.	2000.
Number of poles	16	12	10	6	4
Frequency in cycles per second (N)	16.8	25	42	50	67
Armature diameter (D)	230	170	135	95	70
Gross core length (λ_g)	27	26	25	29	33
Pole pitch (τ)	45	44	42	49	55
$D\times(\lambda_g+0.7\,\tau)$	13,300	9,800	7,300	5,900	5,100
$\frac{\lambda_g}{\tau}$	0.6	0.6	0.6	0.6	0.6
$D^2\times\lambda_g$	1,430,000	750,000	460,000	260,000	160,000
Armature peripheral speed (Sa)	15	22	35	50	72
Armature ampere turns per pole	6,700	6,600	6,300	7,300	7,600
Current per circuit	125	166	200	333	500
Turns per pole (T)	53	40	32	22	15
Number of commutator segments	850	480	320	132	60
Width of each segment	0.5	0.5	0.5	0.5	0.5
Commutator peripheral speed (Sc)	9	10	14	11	12
Reactance voltage	2.8	4.3	6.0	11	18
Total weight (metric tons)	24	15	11	8	6

NOTE. — The peripheral speeds are in metres per second.

The leading data of the selected 500-kw. 250-volt designs for rated speeds of 125, 250, 500, 1000, and 2000 R.P.M. are set forth in Table 54. Corresponding investigations have been made of designs for 500 kw. rated output at these same rated speeds, but for *normal pressures of 500 volts and 1000 volts.* Of the necessary steps in the investigation we shall here merely reproduce the groups of curves in Fig. 257. A number of the groups of curves in this figure are identical with those of Figs. 251 to 255, but there are added groups of curves relating to armature and commutator peripheral speeds at the two new voltages.

The data in Table 56 have been derived from a study of the curves in Fig. 257, selecting the most suitable design for each rated speed and voltage.

In Fig. 259 are brought together curves showing some of the leading data of these fifteen 500-kw. designs. These studies have been supplemented by similar studies for 250-kw. and 1000-kw. designs, and the leading results are set forth in Tables 55 and 57 relating respectively to the 250-kw. and 1000-kw. designs.

Similar curves to those in Fig. 259 have been plotted from Tables 55 and 57, and are given in Figs. 258 and 260.

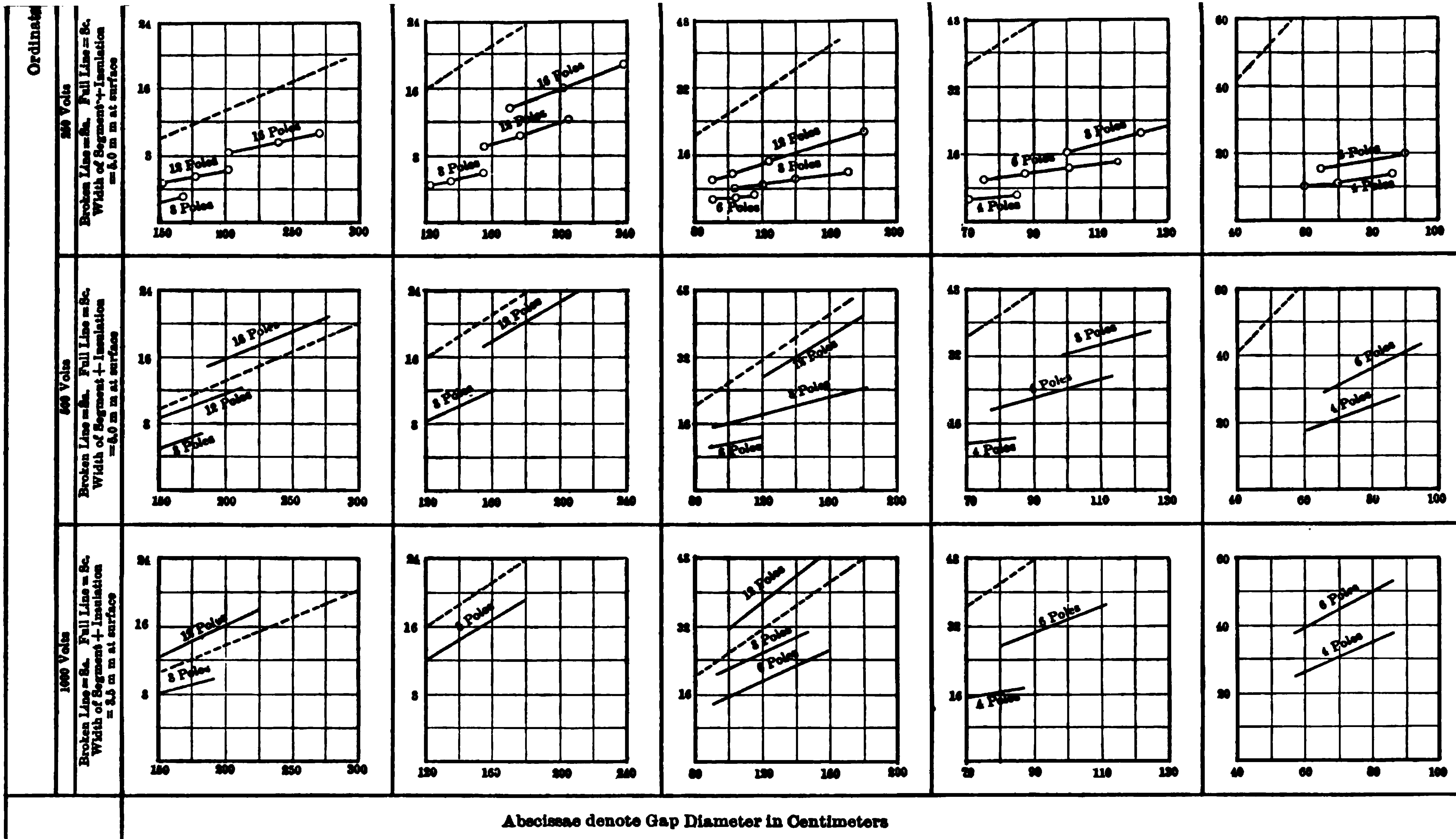


FIG. 257.— Curves relating to preliminary designs, for 250, 500 and 1000-volt 500-kw. continuous current generators for various rated speeds.

lenote: -

Ratio of $\frac{\lambda g}{\tau}$	$D \times (\lambda g + 0.7\tau)$	Reactance Voltage

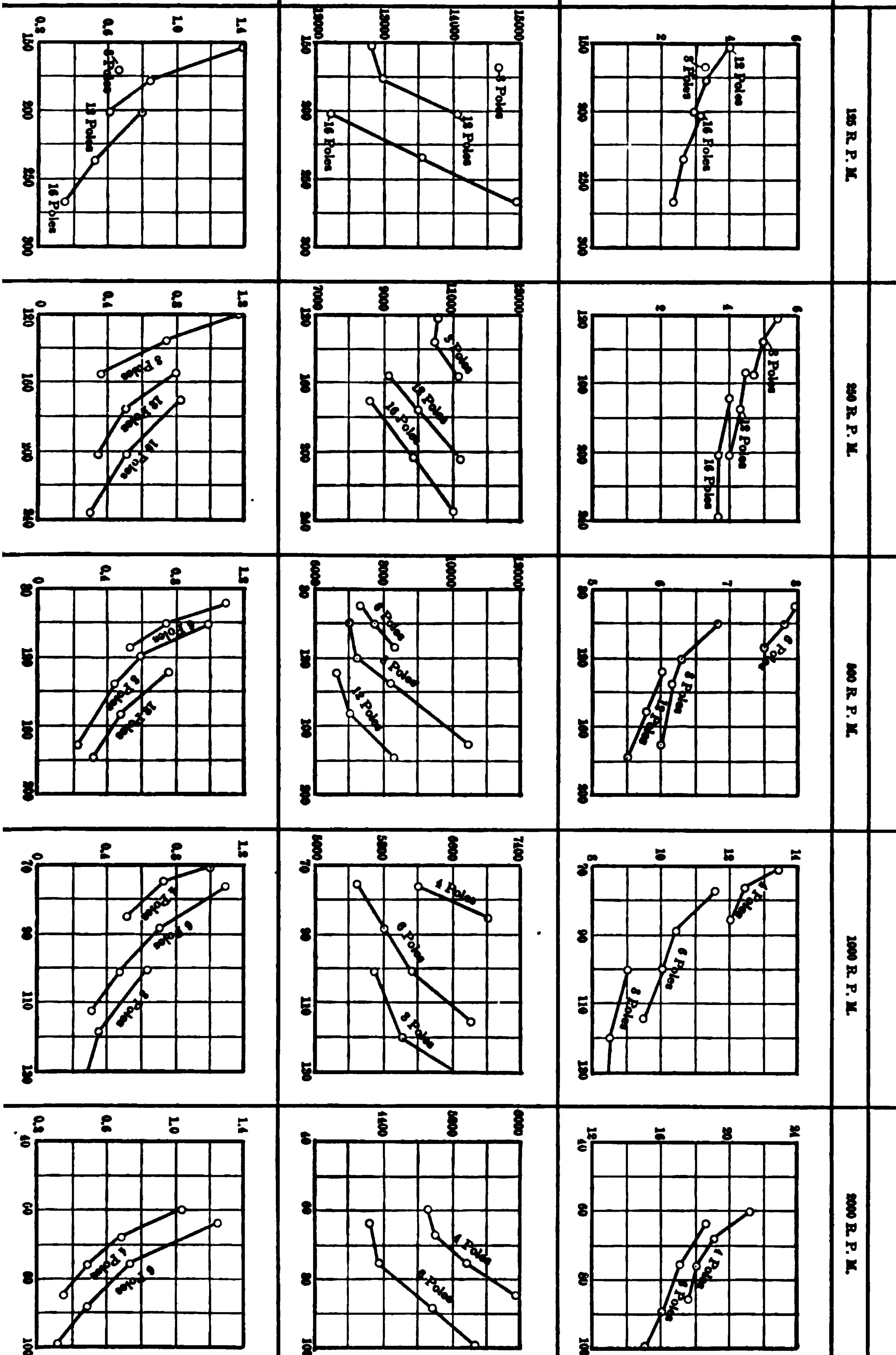


TABLE 55.

DESIGNS SELECTED FROM CHART IN FIG. 258 FOR 250-KW. CONTINUOUS CURRENT GENERATOR FOR 250, 500, AND 1000 AT VARIOUS RATED SPEEDS.

Rated Speed in Revs. per Minute.	125.			250.			500.			1000.			2000.		
Voltage	250	500	1,000	250	500	1,000	250	500	1000	250	500	1000	250	500	1,000
Number of poles	10	8	8	8	6	6	6	6	4	6	6	4	4	4	2
Cycles per second	10.5	8.4	8.4	16.8	12.6	12.6	25	25	16.8	50	50	33.6	67	67	33.6
Armature diameter (D)	160	150	170	120	110	130	85	90	75	75	60	60	55	55	50
Gross core length (λ_g)	30	34	26	28	35	24	31	28	41	24	37	37	30	30	35
Pole pitch (τ)	50	59	67	47	58	69	44	47	59	39	32	47	43	43	43
$D\times(\lambda_g+0.7\,\tau)$	10,400	11,000	12,500	7200	8300	9,200	5200	5400	6000	3800	3600	4200	3300	3300	4,500
$\lambda_g\div\tau$	0.60	0.58	0.39	0.60	0.61	0.35	0.70	0.60	0.70	0.61	1.15	0.80	0.70	0.70	0.43
Armature peripheral speed (Sa)	11	10	11	16	14	17	22	24	20	38	30	30	59	59	53
Armature ampere turns per pole	7,500	8,800	10,000	7000	8700	10,300	6600	7000	8800	5800	4800	7000	6500	6500	11,800
Amperes per circuit	100	63	31	125	84	42	167	84	63	167	84	63	250	125	125
Turns per pole (T)	75	140	320	56	103	246	40	83	140	35	57	110	26	52	95
Turns per segment	1	1	2	1	1	2	1	1	1	1	1	1	1	1	1
Total number of commutator segments	750	1,120	1,280	448	618	738	240	498	560	210	342	440	104	208	190
Width of segment + insulation	0.5	0.35	0.35	0.5	0.5	0.5	0.5	0.5	0.35	0.5	0.5	0.35	0.5	0.5	0.5
Commutator peripheral speed (Sc.)	8.0	8.4	9.5	9.4	13	16	10	21	16	17	28	25	17	34	32
Reactance voltage	2.3	2.4	4.0	3.0	3.7	7.0	4.0	4.4	5.8	6.5	7.8	8.5	13	13	12

TABLE 56.

DESIGNS SELECTED FROM CHART IN FIG. 259 FOR 500-KW. CONTINUOUS CURRENT GENERATORS FOR 250, 500, AND 1000 VOLTS AT VARIOUS RATED SPEEDS.

Rated Speed in Revs. per Minute.	125.			250.			500.			1000.			2000.		
Voltage	250	500	1,000	250	500	1,000	250	500	1000	250	500	1000	250	500	1000
Number of poles	16	12	10	12	10	8	10	8	8	6	6	6	4	4	4
Cycles per second	16.8	12.6	10.5	25	21	16.8	42	33.6	33.6	50	50	50	67	67	67
Armature diameter (D)	230	200	190	170	160	150	135	120	120	95	95	90	70	70	70
Gross core length (λ_g)	24	32	34	26	30	34	25	30	30	29	29	32	33	33	33
Pole pitch (τ)	45	52	60	44	50	58	42	46	46	49	49	46	55	55	55
$D\times(\lambda_g+0.7\,\tau)$	12,600	13,600	14,400	9800	10,500	11,000	7300	7400	7400	5900	5900	5900	5100	5100	5100
$\lambda_g\div\tau$	0.54	0.62	0.57	0.60	0.60	0.60	0.60	0.65	0.65	0.60	0.60	0.70	0.60	0.60	0.60
Armature peripheral speed (Sa)	15	13.0	12.5	22	20	18	35	32	32	50	50	45	72	72	72
Armature ampere turns per pole	6700	7800	9000	6600	7500	8700	6300	6900	6900	7300	7300	6900	7600	7600	7600
Amperes per circuit	125	83	50	166	100	62	200	125	62	333	166	83	500	250	125
Turns per pole (T)	53	94	180	40	75	140	32	55	110	22	44	83	15	30	60
Turns per segment	1	1	1	1	1	1	1	1	1	1	1	1	1	1	1
Total number of commutator segments	848	1130	1800	480	750	1120	320	440	880	132	264	498	60	120	240
Width of segment + insulation	0.5	0.5	0.35	0.5	0.5	0.35	0.5	0.5	0.35	0.5	0.5	0.35	0.5	0.5	0.35
Commutator peripheral speed (Sc)	9.0	12	13	10	16	16	14	19	26	11	22	29	10	20	28
Reactance voltage	2.8	3.0	3.0	4.3	4.6	4.8	6.0	6.5	6.5	11	11	11	18	18	18

TABLE 57.

DESIGNS SELECTED FROM CHART IN FIG. 260 FOR 1000-KW. CONTINUOUS CURRENT GENERATORS FOR 250, 500, AND 1000 VOLTS AT VARIOUS RATED SPEEDS.

Rated Speed in Revs. per Minute.	125.			250.			500.			1000.			2000.		
Voltage	250	500	1,000	250	500	1,000	250	500	1,000	250	500	1000	250	500	1000
Number of poles	20	16	14	16	14	12	12	10	8	8	8	6	6	4	4
Cycles per second	21	16.8	14.7	33.6	29.4	25	50	42	33.6	67	67	50	100	67	67
Armature diameter (D)	320	290	270	225	225	212	175	162	150	135	135	110	76	76	76
Gross core length (λ_g)	27	32.5	37	30	30	34	26.5	31	36	26.5	26.5	36	56	56	56
Pole pitch (τ)	50	57	60	44	50	55	46	51	60	53	53	58	40	60	60
$D\times(\lambda_g+0.7\,\tau)$	20,000	21,000	21,400	13,700	14,600	15,200	10,300	10,800	11,700	8600	8600	8800	6400	7400	7400
$\lambda_g\div\tau$	0.54	0.57	0.62	0.69	0.60	0.62	0.58	0.61	0.60	0.50	0.50	0.67	1.4	0.94	0.94
Armature peripheral speed (Sa)	20	18.5	17.5	27.5	27.5	27	46	43	40	72	72	58	80	80	80
Armature ampere turns per pole	7500	8500	9000	6600	7500	8200	6900	7600	8800	7900	7900	8700	6000	9000	9000
Amperes per circuit	200	125	72	250	143	83	333	200	125	500	250	167	670	500	250
Turns per pole (T)	38	68	125	26	52	100	20	38	70	16	32	52	9	18	36
Turns per segment	1	1	1	1	1	1	1	1	1	1	1	1	1	1	1
Total number of commutator segments	760	1090	1750	416	730	1200	240	380	560	128	256	312	54	72	144
Width of segment + insulation	0.5	0.5	0.45	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5
Commutator peripheral speed (Sc)	8.0	12	17	8.8	15	25	10	16	24	11	22	26	9.0	12	24
Reactance voltage	4.2	4.4	4.6	6.0	6.3	7.0	9.4	10	11	15	20	18	30	35	35

CHAPTER XV.

A SET OF PRELIMINARY DESIGNS FOR CONTINUOUS CURRENT GENERATORS FOR VARIOUS RATED OUTPUTS AND SPEEDS AND FOR THE MOST FAVOURABLE VOLTAGES FOR THESE OUTPUTS.

IN this chapter three sets of designs, each ranging from low to high speeds, have been roughly worked out as an alternative to the method employed in Chapter XIV and partly as a check on that method, for the purpose of determining the consequences of varying the rated speed of continuous current generators of a given rated output.

The designs have been prepared in three sets as follows:

250 kw. 250 volts	125 Revs. per Minute 250 " " " 500 " " " 1000 " " " 2000 " " " 3000 " " "
500 kw. 500 volts	125 Revs. per Minute 250 " " " 500 " " " 1000 " " " 2000 " " " 2500 " " "
1000 kw. 1000 volts	125 Revs. per Minute 250 " " " 500 " " " 1000 " " " 2000 " " "

The designs in the present chapter have not been arrived at by employing the charts given in Chapter XIV, but have, on the contrary, each been independently worked out by a process of successive calculations. Of course the principles were kept in mind, but instead of consulting the charts in question, the designs in the present chapter were evolved independently of the precise assumptions on which the charts are based. While this is a much more laborious plan of procedure, it is nevertheless desirable to approach dynamo designing

undertakings from various standpoints. The 250-kw. 250-volt designs are first dealt with. In all cases except where the contrary is expressly stated, the dimensions in the tabular specifications are given in centimetres.

The highest speed in each case corresponds to a suitable speed for direct connection to a Parsons (*i.e.*, Westinghouse or Allis-Chalmers) type of steam turbine. With continuous current generators of large rated output and low voltage, considerable difficulty is experienced in the design of a suitable commutator, owing to the extreme length. This question is dealt with more fully in a later chapter.

In view of the limitations imposed by commutator design, the voltage for each set of designs has been taken proportional to the output in kilo-

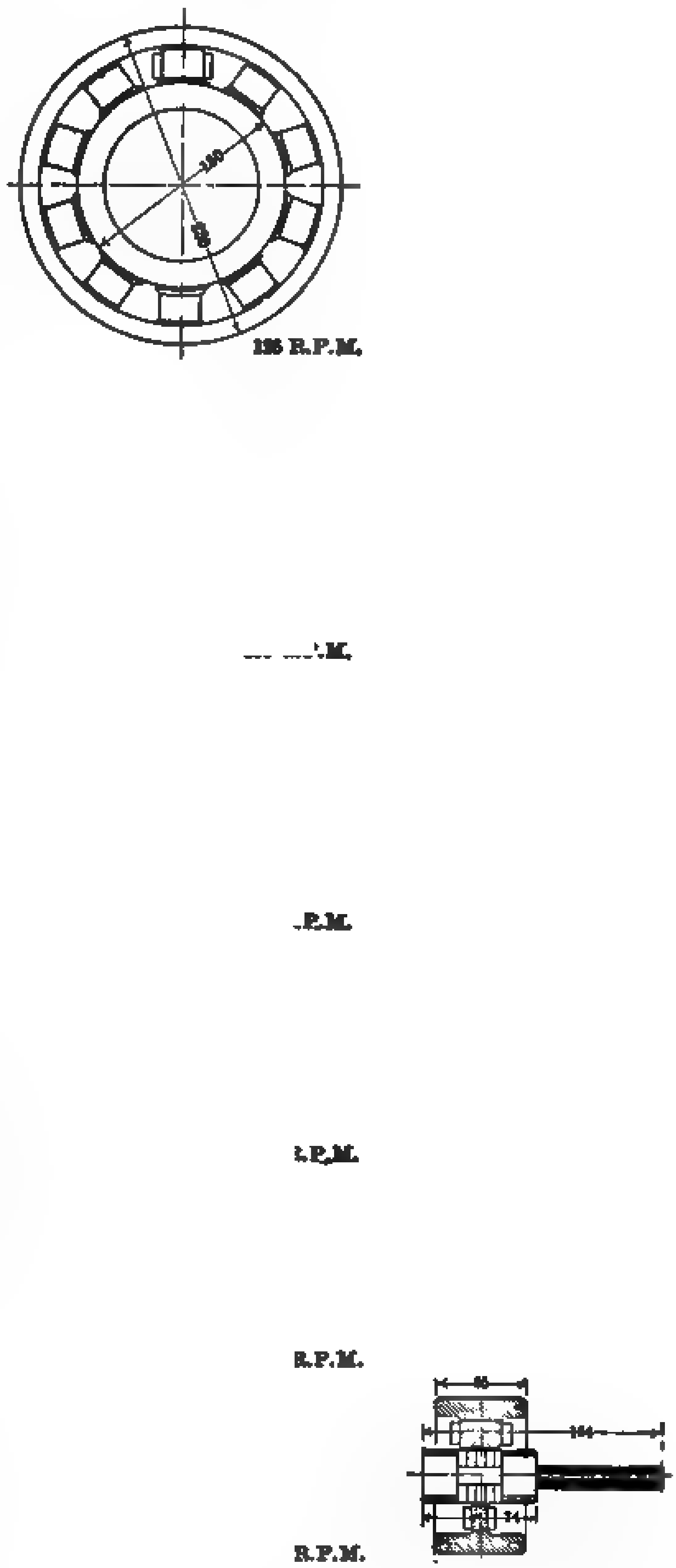


FIG. 261.— Outline sketches of 250-kw. 250-volt continuous current dynamos for rated speeds ranging from 125–3000 r.p.m.

watts. This grading of the voltage permits of less unsatisfactory results as regards commutator dimensions. The data of the designs are set out in the large specification contained in Table 58. Sets of outline sketches, Figs. 261, 262, and 263, on which the leading dimensions are given, accompany the specification. In Fig. 264 all the designs for 125, 250, 500, 1000, and 2000 R.P.M. are brought together on a single chart, which, however, is necessarily on a considerably smaller scale. Nevertheless, it has the advantage of bringing out more clearly the relative dimensions of the various designs of the entire group.

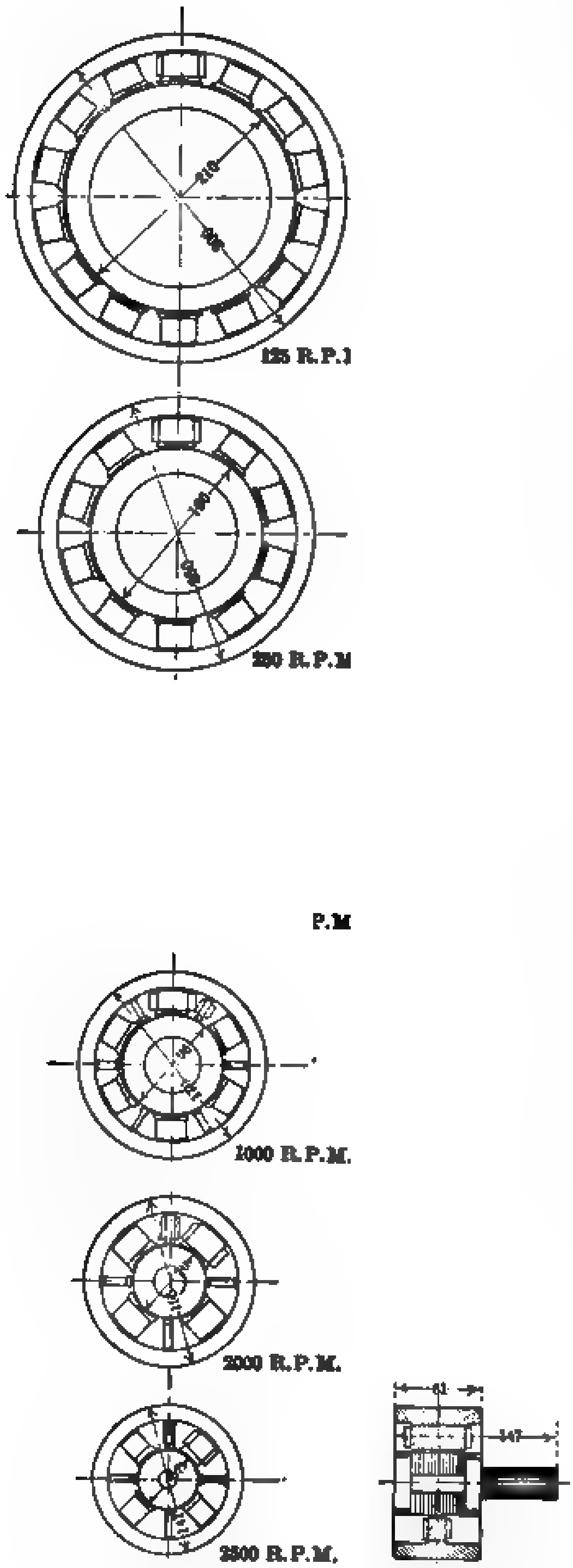


Fig. 262. — Outline sketches of 500-kw. 500-volt continuous current dynamos for rated speeds ranging from 125–2500 r.p.m.

Predetermination of the "Total Works Cost." — On page 353 of Chapter XIV, brief allusion has been made to the question of the estimation of the Total Works Cost. Estimations of this quantity are usually undertaken by the cost department, — a department in no way associated with designing work. The methods used are somewhat cumbersome, involving the pricing up of each separate item in the specification, before a reasonable estimate is obtained. It is obvious,

therefore, that the designer is often greatly inconvenienced by loss of time when he has to depend on the cost department for his estimates of cost.

In order to obviate this difficulty as far as possible, the writers have made a careful study of the principles affecting the Total Works Cost. They have ascertained that the Total Works Cost of a machine is fairly proportional to the product of the armature diameter and the overall length of the armature winding.

The Total Works Cost in dollars is equal to

$$K \times D \times (\lambda g + 0.7\,\tau).$$

The value of K varies somewhat with output and speed, and also varies with different manufacturers according to their workshop and staff facilities; but we are not concerned with the influence of the latter consideration upon the value of K, as the method of estimating the Total Works

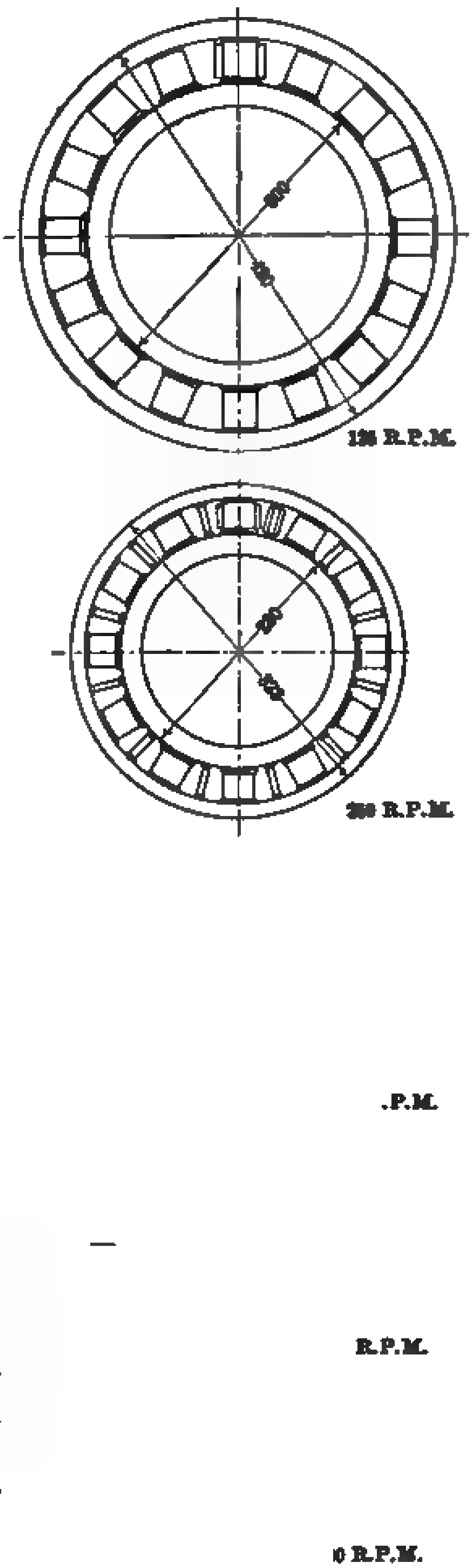


Fig. 263. — Outline sketches of 1000-kw. 1000-volt continuous current dynamos for rated speeds ranging from 125–2000 r.p.m.

TABLE 58. — *Continued.*

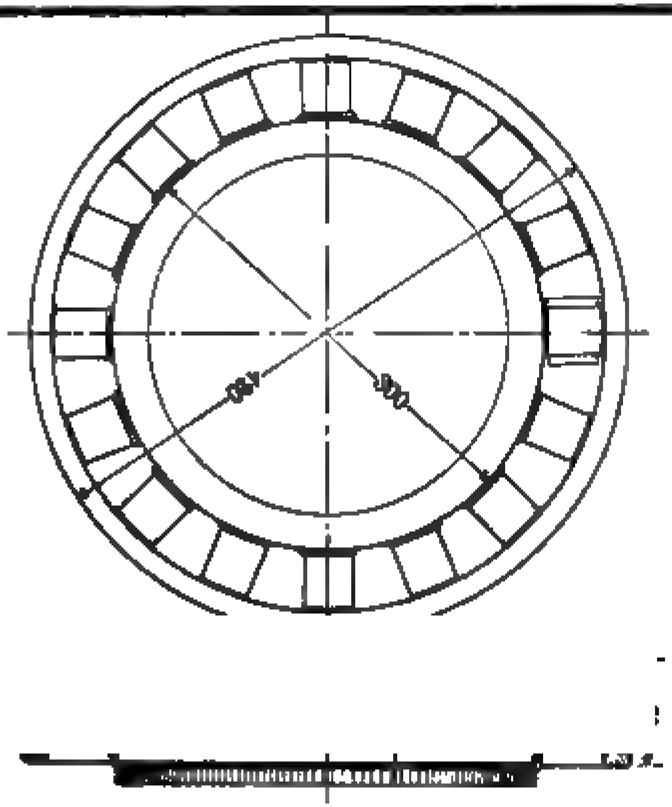
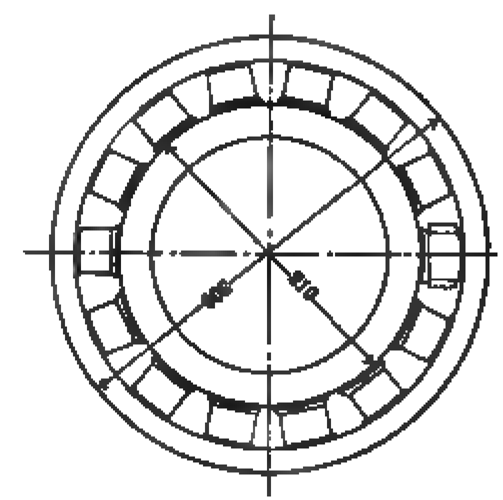
Rated Speed in Revolutions per Minute.	250 Kilowatts.						500 Kilowatts.						1000 Kilowatts.				
	125	250	500	1000	2000	3000	125	250	500	1000	2000	2500	125	250	500	1000	2000
Summary of Ampere Turns at Full Load.																	
Armature core	140	126	126	105	105	126	175	150	160	133	126	150	200	200	200	70	200
Armature teeth	780	1040	1080	1200	920	150	945	950	1040	960	560	560	1900	1230	1230	1720	600
Air gap	3030	3460	3520	3180	2110	2120	6400	5600	3960	4100	2940	3940	7800	5300	4100	4030	3000
Magnet core	810	860	970	810	810	720	960	960	860	960	960	960	1600	1300	1200	1400	1050
Magnet yoke	1120	1120	1320	1150	1460	2520	1060	1150	600	1150	1810	1420	1300	1400	1500	1000	1800
Ampere turns to overcome arma-																	
. . . .	1750	1700	2000	1500	1500	2000	2000	1500	1600	1700	1500	1300	2500	2500	2500	2500	1700
. . . .	7060	8340	9020	7960	6910	7200	11470	9300	8200	9010	7900	8330	15300	11000	11800	10700	8350
t) . . .	5200	5900	6400	6000	5000	4800	8380	6950	6130	6730	6160	6850	12000	8900	8800	7700	6300
l from ... (series) . . .	2460	2440	2620	1960	1910	2400	3090	2350	2070	2280	1740	1480	3300	3000	3000	3000	2000
Ampere turns allowed for shunt winding	5300	6000	6500	6200	5200	5000	8400	7000	6200	6800	6200	6900	12500	9300	9200	8000	6800
Ampere turns allowed for series winding	2500	2500	2700	2000	2000	2500	3100	2400	2100	2300	1800	1500	3500	3500	3500	3500	2200
Shunt Spool Winding.																	
Depth of winding	6	6	6	6	6	7	7	6	6	6	6	6	7	6	6	7	5
Volts per spool	25	31	42	42	62.5	125	36	50	63	83	125	125	62.5	84	125	140	250
Space factor	0.54	0.52	0.5	0.5	0.47	0.47	0.54	0.5	0.52	0.5	0.5	0.5	0.5	0.5	0.5	0.45	0.45
Kgs. copper per spool	40	46	45	33	24	37	67	55	43	41	44	56	116	65	72	78	63
Watts lost per spool	105	170	190	165	153	180	288	225	210	263	218	298	450	350	350	310	360
Series Spool Winding.																	
Depth of winding	6	6	6	6	6	6	6	6	6	6	6	6	6	6	6	6	5
Turns per spool	3.5	3.5	4.0	3.0	3.0	3.5	4.5	3.5	3.0	3.0	2.5	2.0	5	5	5	5	3
Kgs. copper per spool	20	20	20	13	13	19	27	21	17	17	15	14	32	29	31	34	24
Watts lost per spool	74	74	74	49	49	74	100	78	72	72	65	53	122	112	117	130	90
Interpoles.																	
Length of pole	*	*	26	22	22	20	*	*	26	26	20	26	*	30	31	27	27
Cross section	...	...	55	50	55	55	...	...	64	75	90	90	...	84	110	120	148
Depth of winding	...	...	4	4	4	6	...	...	4	3.5	3.5	3.0	...	4	4	5	4
Turns per spool	...	...	13.5	11.0	11.0	15.0	...	...	13	10.5	11.5	10.0	...	18	18	18.5	13
Kgs. copper per spool	...	...	28.5	21.7	23.0	27.5	...	...	27.5	24.4	29.5	24.5	...	44	52.5	67	47.5
Watts lost per spool	...	...	162	118	138	100	...	...	156	140	165	114	...	196	235	344	210

Constant Losses.																	
Armature iron loss	3600	3900	3600	4800	4600	4100	9200	9000	9600	11000	11500	11000	19500	15000	16500	14000	20000
Brush friction loss	650	950	1300	1700	2000	2300	1200	1300	1500	2400	2500	2500	2200	2300	2500	2600	3400
Shunt losses	1350	1360	1140	790	600	360	4030	2250	1680	1680	870	1190	7200	4200	2800	1860	1440
Friction and windage loss	2500	2500	2500	2500	2500	2500	5000	5000	5000	5000	5000	5000	10000	10000	10000	10000	10000
Total constant losses	8400	8710	8540	9790	9700	9260	19430	17550	17780	19080	19870	19690	38900	31500	31800	28460	34840
Variable Losses.																	
Armature I^2R	10800	7300	6000	4100	2800	2800	13600	10500	8100	5300	3800	2650	23000	21000	15000	10600	4700
Brush I^2R	2000	2000	2000	2000	2000	2000	2000	2000	2000	2000	2000	2000	3000	3000	3000	3000	3000
Main series I^2R	1050	850	630	430	290	210	2600	1440	1200	830	480	390	2800	1940	1340	1100	510
Interpole series I^2R	*	*	1220	910	680	410	*	*	1600	1100	820	630	*	3400	2700	2080	1200
Total variable losses	13850	10150	9850	7440	5770	5420	18200	13940	12900	9230	7100	5670	28800	29340	22040	16780	9410
Total of all Losses	22250	18860	18390	17230	15470	14680	37630	31490	30680	29210	26970	25360	67700	60840	53840	45240	44250
Full load efficiency %	92.0	92.8	93.3	93.7	94.2	94.6	93.0	94.0	94.4	94.6	95.0	95.1	94.0	94.5	95.0	95.5	95.8
½ load efficiency %	89.5	90.0	90.0	90.0	90.0	92.0	91.0	92.0	92.2	92.2	92.2	92.2	91.0	92.0	93.0	93.0	93.0
¼ load efficiency %	85.0	85.0	85.0	85.0	85.0	86.0	85.0	86.0	86.0	86.0	86.0	86.0	86.0	87.0	88.0	88.0	88.0
Heating Constants in Watts per Square Decimetre.																	
Armature	51	56	60	72	82	85	53	60	74	86	95	96	61	68	80	91	98
Commutator	32	38	46	56	64	70	38	40	45	58	68	74	48	55	57	69	72
Shunt spools	9.5	9.5	9.5	10.0	11.0	11.0	10.0	10.0	10.0	10.5	10.5	11.0	10.5	10.5	10.5	10.5	10.5
Series main	11.0	11.0	11.0	11.0	11.0	11.0	11	11	11	11	11	11	11	11	11	11	11
Series interpole	*	*	12.0	12.0	12.0	12.0	*	*	12	12	12	12	*	12	12	12	12
Weights of Effective Materials (Kgs.).																	
	278	186	156	105	100	100	350	260	220	136	98	68	600	540	380	260	180
	340	266	236	207	185	122	500	480	440	400	280	220	750	660	600	500	470
	400	370	270	200	96	74	940	550	344	246	176	224	1850	780	680	470	250
	200	160	120	78	52	38	378	210	136	102	60	56	510	350	250	200	100
r	*	*	170	130	92	55	*	*	220	146	117	89	*	530	420	400	190
	4500	3300	2750	1420	1190	1610	7500	6000	4200	3450	3420	3350	12500	7400	6500	6000	6000
el)	1200	940	677	385	261	218	2360	1600	1196	850	670	720	5000	2200	1760	1140	1100
teel)	*	*	70	55	42	18	*	*	104	100	80	80	*	230	200	150	120
	1300	850	520	300	190	190	2300	1500	960	650	500	500	3900	2100	1650	1200	1000
	8200	6070	4970	2880	2210	2425	14330	10600	7280	6080	5400	5310	25110	14790	12240	10520	9410
Total effective weight per kw.	32.9	24.3	19.9	11.5	8.8	9.7	28.6	21.2	15.6	12.1	10.8	10.6	25.1	14.8	12.2	10.5	9.4
Costs of Effective Materials (Dollars).																	
Copper	730	585	570	430	314	235	1300	900	816	618	438	394	2220	1720	1340	1190	714
Cast iron	167	122	102	52	44	60	277	220	155	128	126	124	462	274	240	222	222
Cast steel	102	80	60	37	26	20	200	136	110	81	64	68	425	206	167	110	104
Laminations	195	128	178	45	29	29	345	225	144	98	90	90	585	315	248	180	150
Total cost of effective material — dollars	1194	915	814	564	413	344	2120	1480	1230	930	720	680	3690	2520	2000	1700	1190
Total cost of effective material — dollars per kilowatt	4.8	3.7	3.2	2.3	1.65	1.4	4.25	2.96	2.45	1.85	1.44	1.35	3.7	2.5	2.0	1.7	1.2

* These designs have no interpoles.

TABLE 58.—*Continued.*

Rated Speed in Revolutions per Minute.	250 Kilowatts.						500 Kilowatts.				1000 Kilowatts.		
	125	250	500	1000	2000	3000	125	250	500	2500	500	1000	2000
Technical Data and Constants.													
Reactance voltage	2.1	3.1	4.7	6.3	10.0	14.4	2.8	4.0	6.8	16.0	12.5	20.0	28.0
Volts per segment	3.6	5.0	6.0	7.5	10.0	14.3	5.8	8.3	9.3	31.0	13.3	18.5	40.0
Armature ampere turns per pole	7000	6250	7000	5600	6250	8750	6000	6000	6750	4000	9400	9000	6300
Armature ampere turns per cm. of periphery	150	145	148	140	145	140	110	120	140	90	155	170	115
Flux per sq. cm. of total air gap surface (kilolines) $= \frac{\text{Flux per pole}}{\tau \times \lambda g}$	6.7	6.1	5.3	4.8	4.0	3.8	7.0	7.0	5.2	4.3	4.4	3.6	3.7
Peripheral speed of armature in metres per second (Sa)	10	15	23	40	57	62	14	21	31	75	40	53	75
$D^2 \times \lambda g$ in decimetres	630	360	200	118	69	50	1420	770	435	160	900	500	370
Output coefficient	3.2	2.8	2.5	2.1	1.8	1.65	2.8	2.6	2.3	1.25	2.2	2.0	1.33
Watts output per kg. of effective material	30.5	41	50	87	114	103	35	47.2	64.5	94	81	96	106
Watts output per cubic cm. of active belt	6.5	6.3	9.3	13.3	17.0	27.0	6.9	9.7	11.4	17	15.5	16.6	18.2
$\frac{\lambda n}{\lambda g}$	0.77	0.75	0.68	0.64	0.60	0.77	0.73	0.72	0.68	0.6	0.7	0.72	0.6
$\frac{\lambda g}{\tau}$	0.6	0.67	0.53	0.54	0.54	0.5	0.68	0.6	0.64	1.1	0.66	0.95	1.36
$\frac{D}{\lambda g}$	5.36	3.67	3.6	3.6	2.4	1.3	6.6	5.3	4.0	1.15	3.75	2.0	0.93
Total Works Cost Calculations.													
$\lambda g + 0.7\,\tau$	60	60	58	48	53	74	65	65	63	82	82	88	114
$D \times (\lambda g + 0.7\,\tau)$	9000	6600	5200	3600	2900	3000	13700	10400	7600	4700	12300	8800	7980
Values of K in formula T.W.C. $= K \times D \times (\lambda g + 0.7\,\tau)$	0.26	0.26	0.26	0.29	0.35	0.40	0.31	0.31	0.32	0.60	0.37	0.50	0.64
T.W.C. in dollars	2300	1720	1350	1040	1020	1200	4250	3220	2430	2800	4500	4400	5100

1000 Kw, 1000 Volts.	500 Kw, 500 Volts.	250 Kw, 250 Volts.	
			125 R.P.M.

Cost as here set forth, is chiefly of value for comparative purposes of alternative designs.

For a given output and speed, K is fairly constant over a fairly wide range of voltage, the extra costs involved in insulation and other details of the higher voltage machines being compensated for by the reduced outlay for the commutator.

In Fig. 265 are plotted values of K for different rated outputs, in terms of the armature peripheral speed in metres per second. For a given output, K is fairly constant up to a peripheral speed of 40 metres per second, but when this value is exceeded, K increases rap-

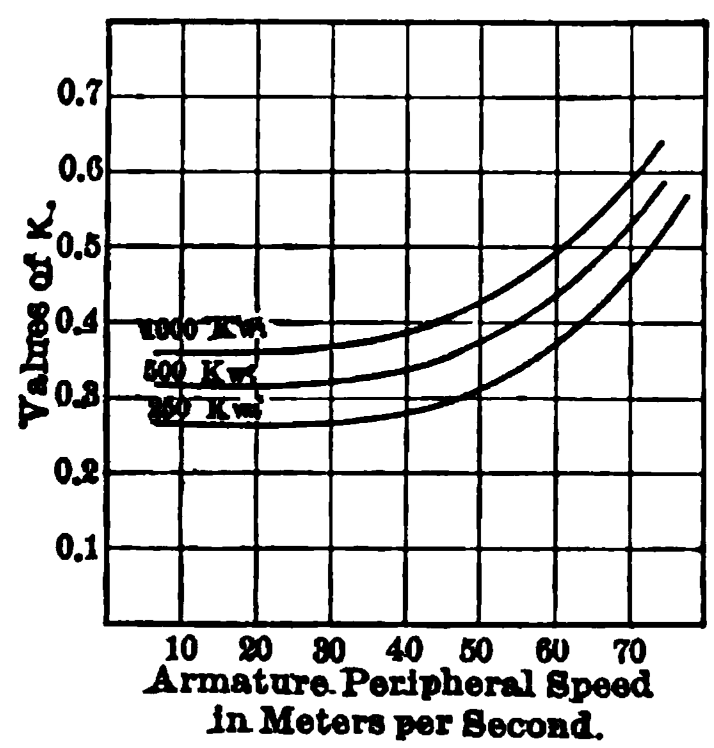


FIG. 265.— Values of "K" in the formula Total Works Cost in dollars = K × D × (λg + 0.7 τ).

idly, owing to increased manufacturing cost and restricted choice of material suitable for withstanding the greater centrifugal forces.

For the 250-kw., 500-kw., and 1000-kw. designs, contained in the preceding specification, the necessary constants for estimating the Total Works Cost have been entered up in the last few lines.

The method of estimating the Total Works Cost is illustrated (as applied to the 250 kw. designs), for the different rated speeds in Figs. 266 to 269. In Fig. 266 the values of $D \times (\lambda_g + 0.7\,\tau)$ are plotted in terms of rated speed. In Fig. 267, the armature peripheral speeds in metres per second are plotted. In Fig. 268, values of K are plotted in terms of the *rated* speed from the values of K which are plotted in terms of *peripheral* speed in Fig. 265.

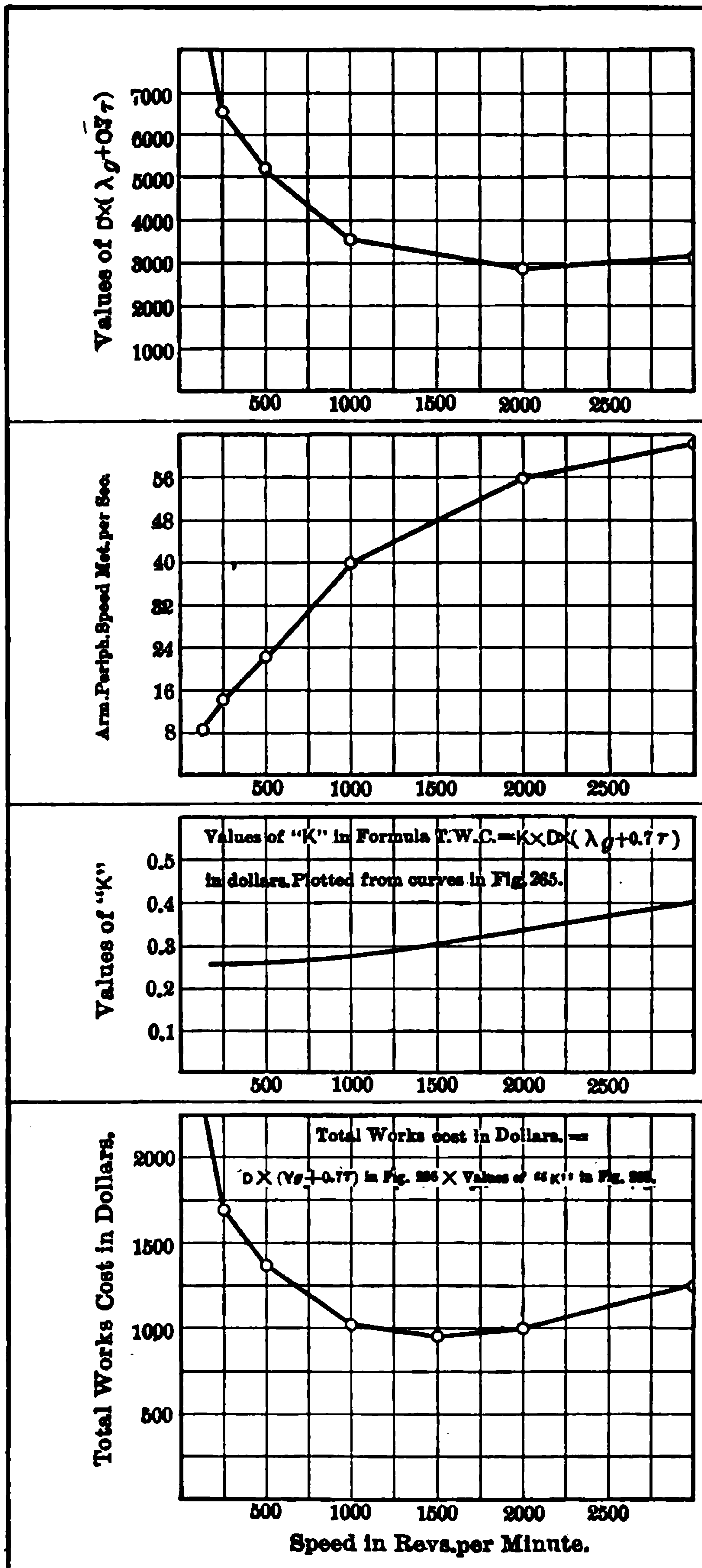


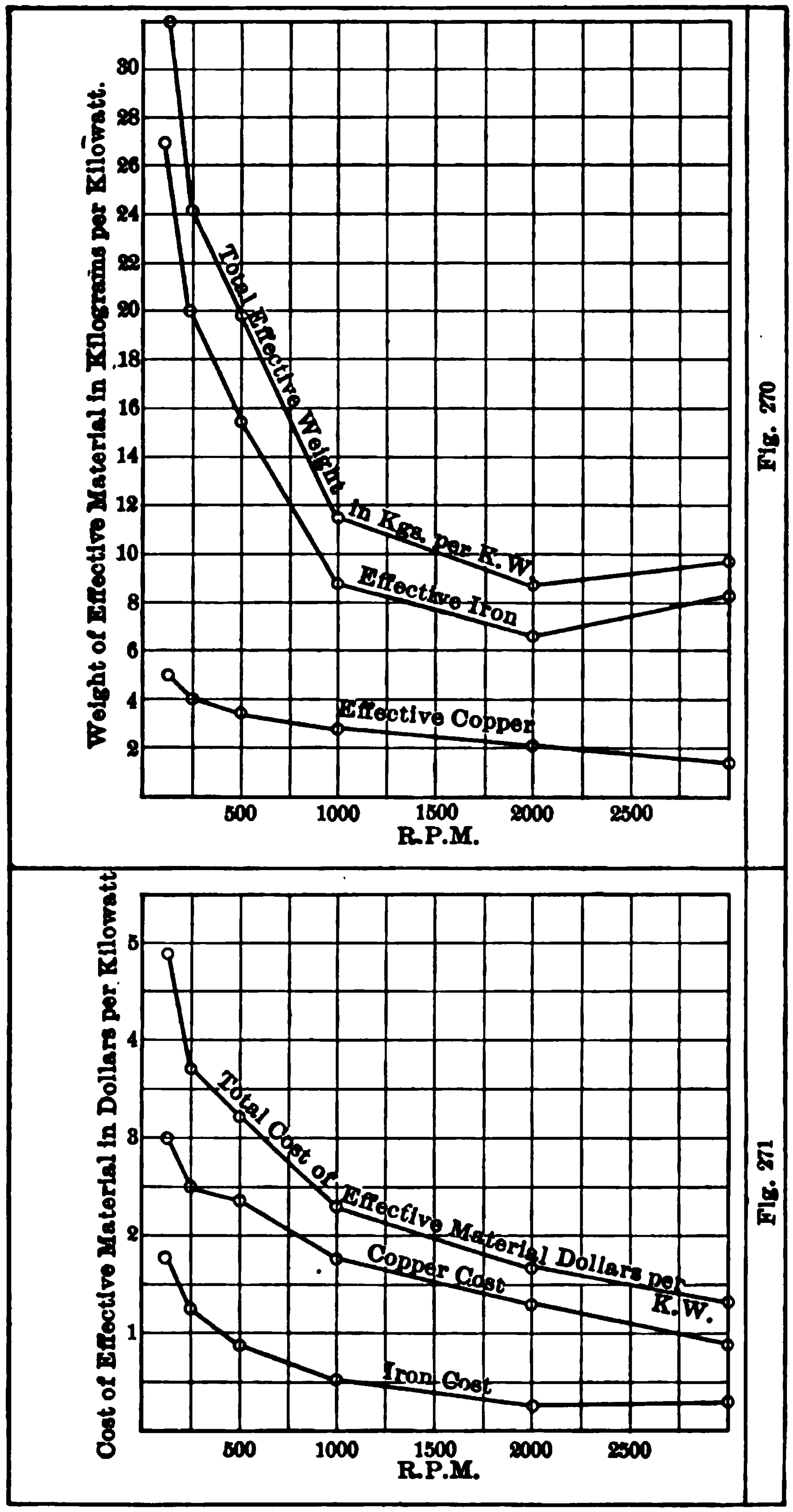
Fig. 266

Fig. 267

Fig. 268

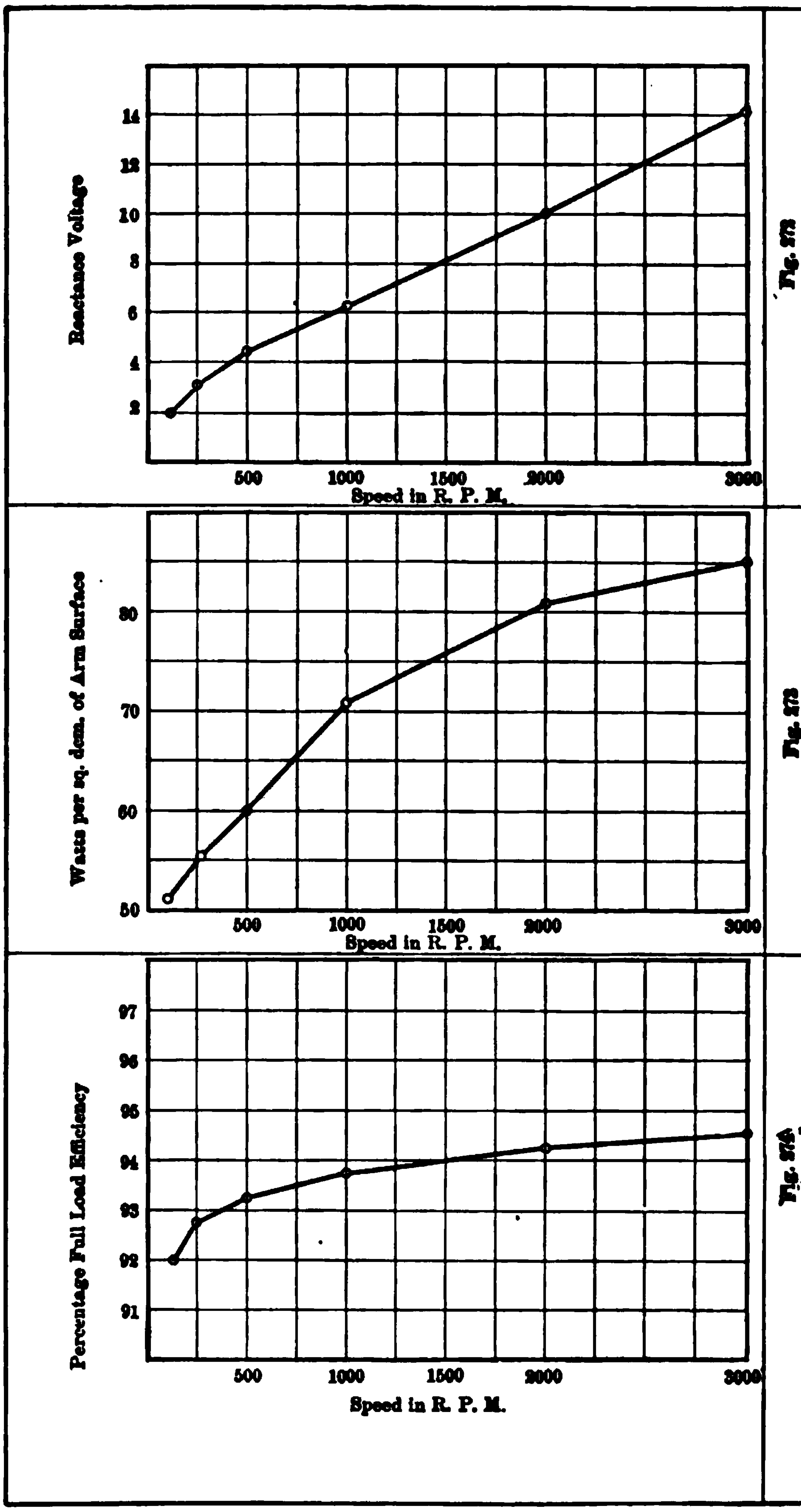
Fig. 269

Figs. 266-269-Graphic Determination of Total Works Cost of 250 Kw. Continuous Current Dynamos for various Rated Speeds.



Figs. 270, 271.— Graphic determination of effective costs and effective weights of 250-kw. 250-volt continuous current dynamos at different rated speeds.

Curves have also been plotted in Figs. 270 to 274, showing the effect of the rated speed on the *effective* weight and cost, the com-



Figs. 272-274. Graphic Determination of Reactance Voltage, Armature Heating, & Full Load Efficiency of 250 Kw. 250 Volt Continuous Current Dynamo for various Rated Speeds.

mutation, heating and efficiency of the 250 kw. designs. These figures relate respectively to: —

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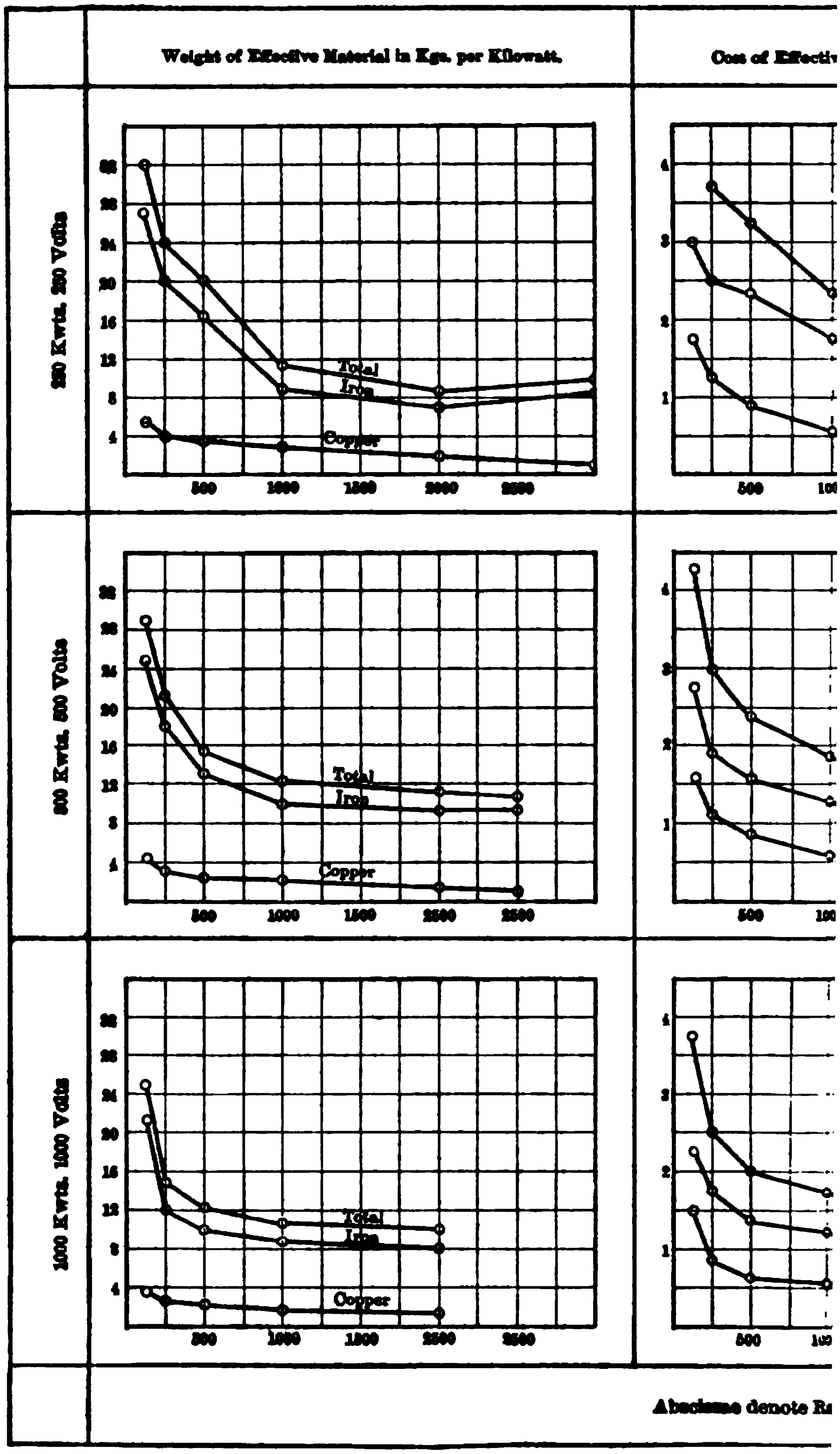

FIG. 275.— Curves showing effective weight, cost of effe
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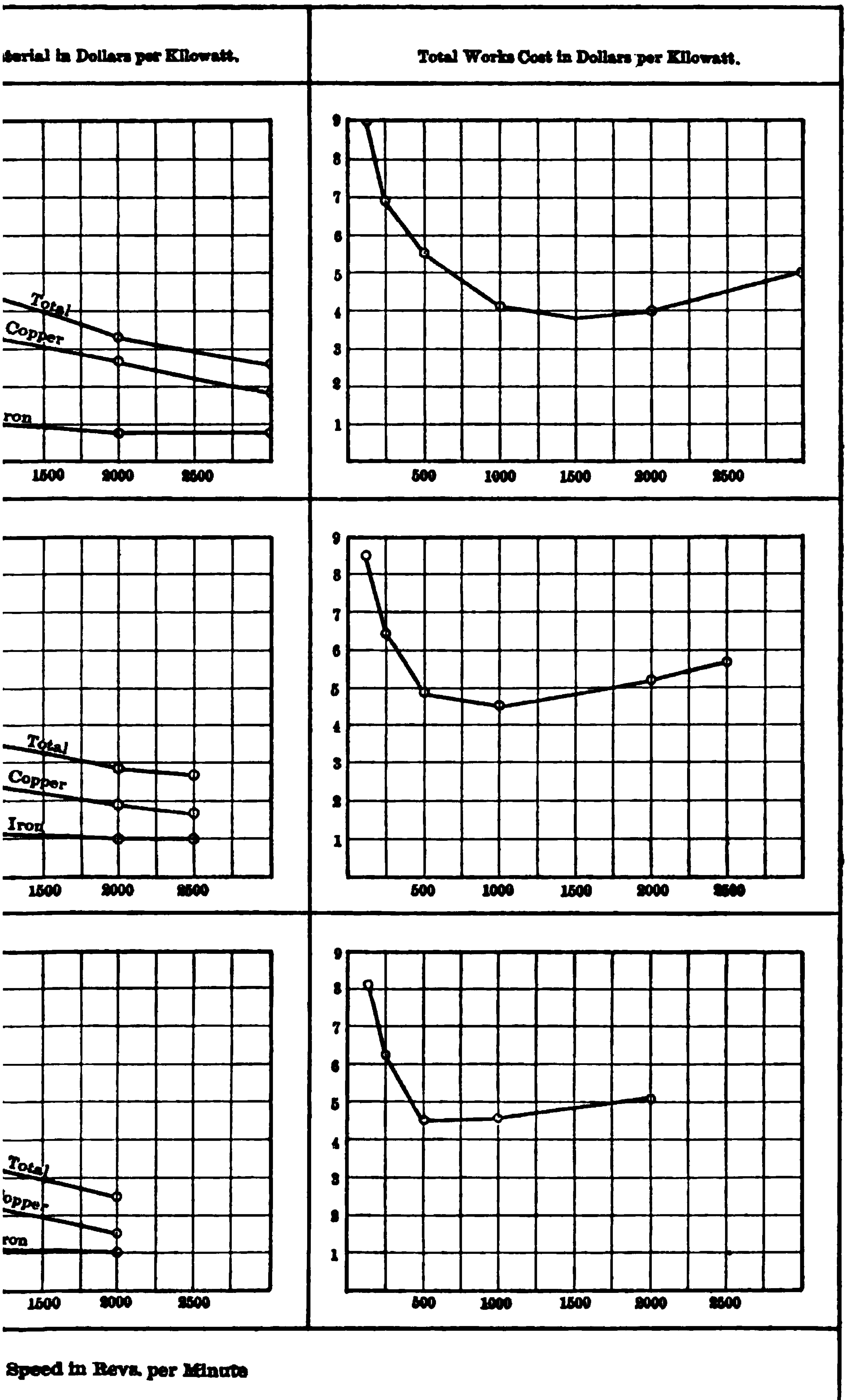

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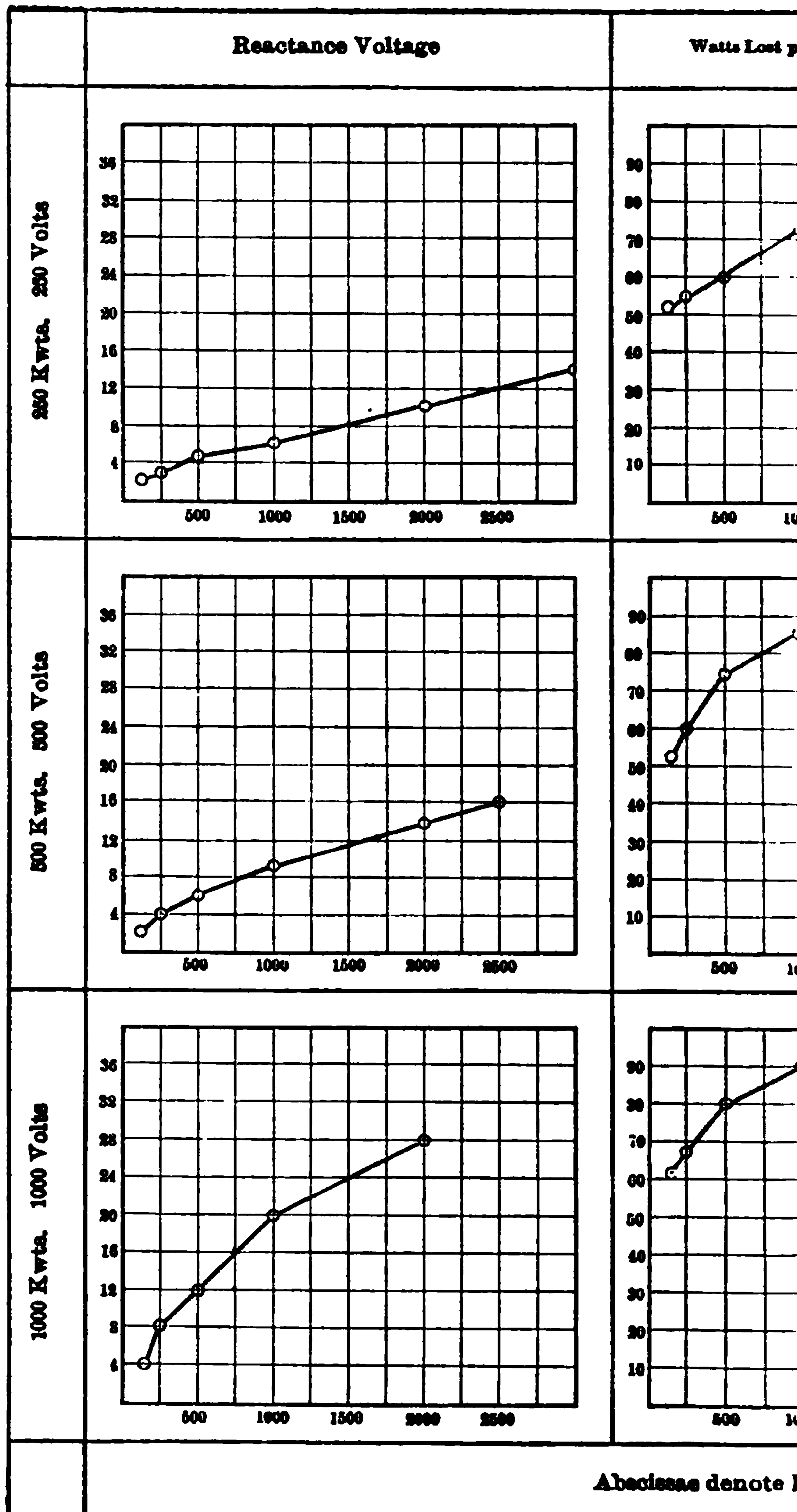

Fig. 276. — Curves showing technical data of 250, 500 and

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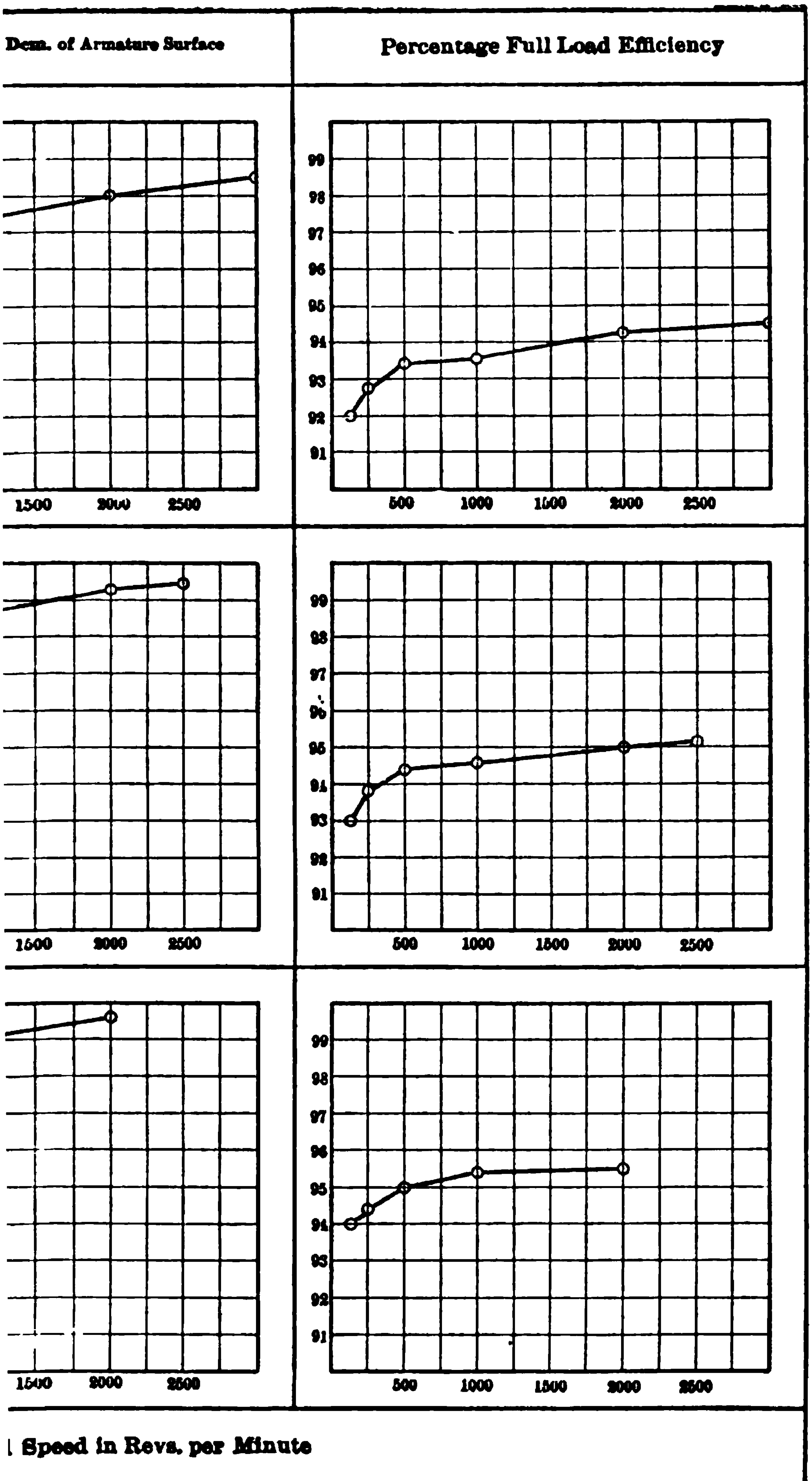


)0 kw. continuous current dynamos for various rated speeds.

Total weight of effective material in kilograms per kilowatt, Fig. 270.

Total cost of effective material in dollars per kilowatt, Fig. 271.

Reactance voltage, Fig. 272.

Armature heating in watts per square decimetre, Fig. 273.

Full load efficiency, Fig. 274.

In the large charts of Fig. 275 and 276 are brought together not only a number of the curves relating to the 250-kw. designs (which have already been given in Figs. 269 to 274), but also, (in the second and third horizontal rows), the corresponding data for the 500-kw. and the 1000-kw. designs.

It cannot be too strongly impressed upon the reader that none of the designs of these groups are to be regarded as more than superficial and preliminary. It is true, a large amount of time has been devoted to their preparation; nevertheless, no experienced designer, when in receipt of a requisition for a design of some specified rating, sends off any design he may chance to have amongst his notes, although he may, and often does, take such a design as a rough trial basis from which a final design is only evolved after days or weeks of detailed work.

Taken with these distinct qualifications, however, this large group of designs is of considerable use, but the main purpose of its preparation has been to obtain a broad view of the varying influence of the two main factors of rated speed and rated output, and of the third and hardly less important factor of rated voltage.

CHAPTER XVI.

A COMPARATIVE STUDY OF THE DESIGNS SET FORTH IN THE TWO PRECEDING CHAPTERS.

THE data of the designs which we have described in Chapter XV. permit of determining roughly the most economical rated speed, with due regard to satisfactory operation and general reliability, for continuous current dynamos of any reasonable output and voltage. In such determinations due regard must be paid to arriving at a compromise between cost, efficiency, commutation, thermal considerations and general reliability, the last named being of chief importance.

In making such an investigation, in order to readily compare the leading constants for a given rated output, but at various rated speeds, considerable assistance may be derived from plotted curves of the various quantities involved. Let us, for example, endeavour to ascertain the most economical speed for a 250-kw. 250-volt continuous current generator. In Fig. 269, of Chapter XV, is plotted, as a function of the rated speed in R.P.M., the Total Works Cost in dollars for generators of this output and voltage. The minimum value of the Total Works Cost is observed to occur at the rated speed of 1500 R.P.M., and may be taken as some \$960.

Fig. 271 shows that the *cost of effective material* at 1500 R.P.M. amounts to some \$2 per kilowatt. The cost of effective material does not reach a minimum value on the curve as drawn. Beyond a rated speed of 1500 R.P.M., however, the decrease is less marked. Thus the total cost of effective material for the 250-kw. 250-volt dynamo is $2.00 \times 250 = \$500$.

The total weight of effective material, expressed in kilograms per kilowatt, reaches its minimum value at 2000 R.P.M., as shown by the curve in Fig. 270. The weight of effective material for a 250-kw. dynamo at a rated speed of 1500 R.P.M. (Fig. 270) is about 10 kilograms per kilowatt, hence the total weight of effective material amounts to $250 \times 10 = 2500$ kilograms = 2.5 metric tons.

From Fig. 272 it is seen that at a rated speed of 1500 R.P.M., a value of some 8 reactance volts is obtained. This is a moderate value for an interpole machine of the rated output under consideration.

The armature heating, expressed in watts per square decimetre at the rated speed of 1500 R.P.M., is about 75 (from the curve in Fig. 273). This value need not lead to an excessive temperature rise, as with an armature peripheral speed of 48 metres per second (see Fig. 267) an excellent circulation of air may be maintained, provided there be a sufficiently large internal diameter of armature and sufficiently numerous ventilating ducts in the core. The armature peripheral speed is 48 metres per second.

The principal constants and data of the 250-kw. 250-volt machine at the most economical rated speed, namely, 1500 R.P.M., are contained in Table 59.

TABLE 59.

TECHNICAL DATA, INCLUDING COST AND WEIGHT, FOR A 250-KW. 250-VOLT 1500 R.P.M. MACHINE.

Total Works Cost in dollars	960
Cost of effective material in dollars per kw.	2.00
Weight of effective material in kg. per kw.	10
Armature peripheral speed in metres per second	48
Commutator peripheral speed in metres per second	26
Reactance voltage	8
Watts per sq dcm of armature surface	76
Efficiency at rated load	94%

In Table 60 some of the leading constants have been deduced for the 500-kw. 500-volt machines, from the curves in Figs. 275 and 276.

The minimum Total Works Cost for a dynamo of 500 kw. rated output, at a terminal pressure of 500 volts, occurs at a rated speed of about 1000 R.P.M.

TABLE 60.

Technical Data, Including Cost and Weight, for a 500-Kw. 500-Volt 1000 R.P.M. Continuous Current Dynamo.

Total Works Cost in dollars	2300
Cost of effective material in dollars per kw.	1.90
Weight of effective material in kg. per kw.	12
Armature peripheral speed in metres per second	50
Commutator peripheral speed in metres per second	30
Reactance voltage	9
Watts per sq dcm of armature surface	85
Efficiency at rated load	94.7%

In Table 61 similar data and constants have been deduced from curves in Figs. 275 and 276 for a machine of 1000 kw. rated output and 1000 terminal volts, and having the most economical speed. In this case the rated speed corresponding to the minimum Total Works Cost, occurs midway between 500 and 1000 R.P.M., and is here taken at 750 R.P.M.

TABLE 61.

Technical Data, Including Cost and Weights, for a 1000-Kw. 1000-Volt 750 R.P.M. Continuous Current Dynamo.

Total Works Cost in dollars	4200
Cost of effective material in dollars per kw.	1.75
Weight of effective material, kg. per kw	11.5
Armature peripheral speed in metres per second	45
Commutator peripheral speed in metres per second	30
Reactance voltage	16
Watts per sq dcm of armature surface	85
Efficiency at rated load	95.2%

The most economical speeds consistent with good designs for these different outputs may thus fairly be taken somewhat as follows:

250 kw.	1500 R.P.M.
500 kw.	1000 R.P.M.
1000 kw.	750 R.P.M.

The steam turbines at present in use for corresponding outputs run at much higher speeds. These speeds are approximately as shown in Table 62.

TABLE 62.

SPEEDS OF DYNAMOS AND TURBINES, IN REVOLUTIONS PER MINUTE.

Kilowatts Output.	Economical Speed of Continuous Current Generator.	Curtis Type Turbine.	Parsons Type Turbine.
250	1500	2200	3000
500	1000	1500	2500
1000	750	1000	2000
2000	500	750	1250

In this table it is shown that, for a continuous current dynamo of given rated capacity, the corresponding economical speed is approximately equal to the speed of a Curtis turbine of twice the rated output. Hence, where direct coupled, steam turbine driven, continuous current sets are required, an obvious plan consists in employing two dynamos direct coupled to one turbine, the turbine being of sufficient rated output to run the dynamos when both are fully loaded, and to meet all requirements of overload during the specified time.

In a large continuous current generating station, this arrangement of coupling two continuous current dynamos on one turbine shaft has, amongst other obvious disadvantages, the disadvantage of complicating the switchboard arrangements, and increasing the amount of supervision required owing to the larger number of electrical generating units employed.

For a lighting station with a widely varying load, the increased number of relatively smaller machines can be turned to good account by improving the all-day efficiency. In Table 63 are set forth the efficiencies of 250, 500, and 1000-kw. continuous current generators at full, half, and quarter load.

TABLE 63.

EFFICIENCIES OF CONTINUOUS CURRENT GENERATORS AT DIFFERENT LOADS.

	Full Load.	One-Half Load.	One-Fourth Load.
	Per Cent.	Per Cent.	Per Cent.
250 kilowatt	94	90	84
500 kilowatt	94.7	92	86
1000 kilowatt	95.2	93.5	88

Let us take the case of a continuous current turbine unit with a rated output of 1000 kw. from the electrical side. Table 64 shows the dynamo efficiencies of two sets of this output, employing one dynamo of 1000 kw. rated output, and two dynamos of 500 kw. rated output respectively.

TABLE 64.

EFFICIENCIES OF 1000-KW. CONTINUOUS CURRENT TURBO-GENERATOR SETS.

	Employing One Dynamo.	Employing Two Dynamos.
Efficiency at full load	95.2	94.7
Efficiency at ½ full load	93.5	94.7
Efficiency at ¼ full load	88	92

From this table it is seen that the efficiency is considerably improved on light loads by using the turbine set with two dynamos. These efficiencies are based on the assumption that at one half the rated load of 500 kw. and at less loads, one of the 500-kw. machines is switched out, its field circuit opened, and its brushes raised.

COMPARISON BETWEEN THE DESIGNING METHODS OF CHAPTERS XIV AND XV.

At this point a brief outline of the results arrived at by the conventional methods of design as illustrated in Chapter XV compared with the preliminary data obtained from the chart in Fig. 247 of Chapter XIV, will be of interest. The simplest way to compare these two different methods of arriving at an approximate preliminary design, is illustrated in Fig. 277, where two sets of curves are plotted, a full line set and a broken line set. These represent respectively the designs worked out by ordinary methods and brought together in Table 58, and those worked out by the chart method and entered up in Tables 55, 56, and 57.

The values of some of the more important data have been plotted in terms of the rated speed.

On the whole, the curves are in good agreement with each other, the chief differences occurring at the higher rated speeds. Even then the differences are relatively slight in amount.

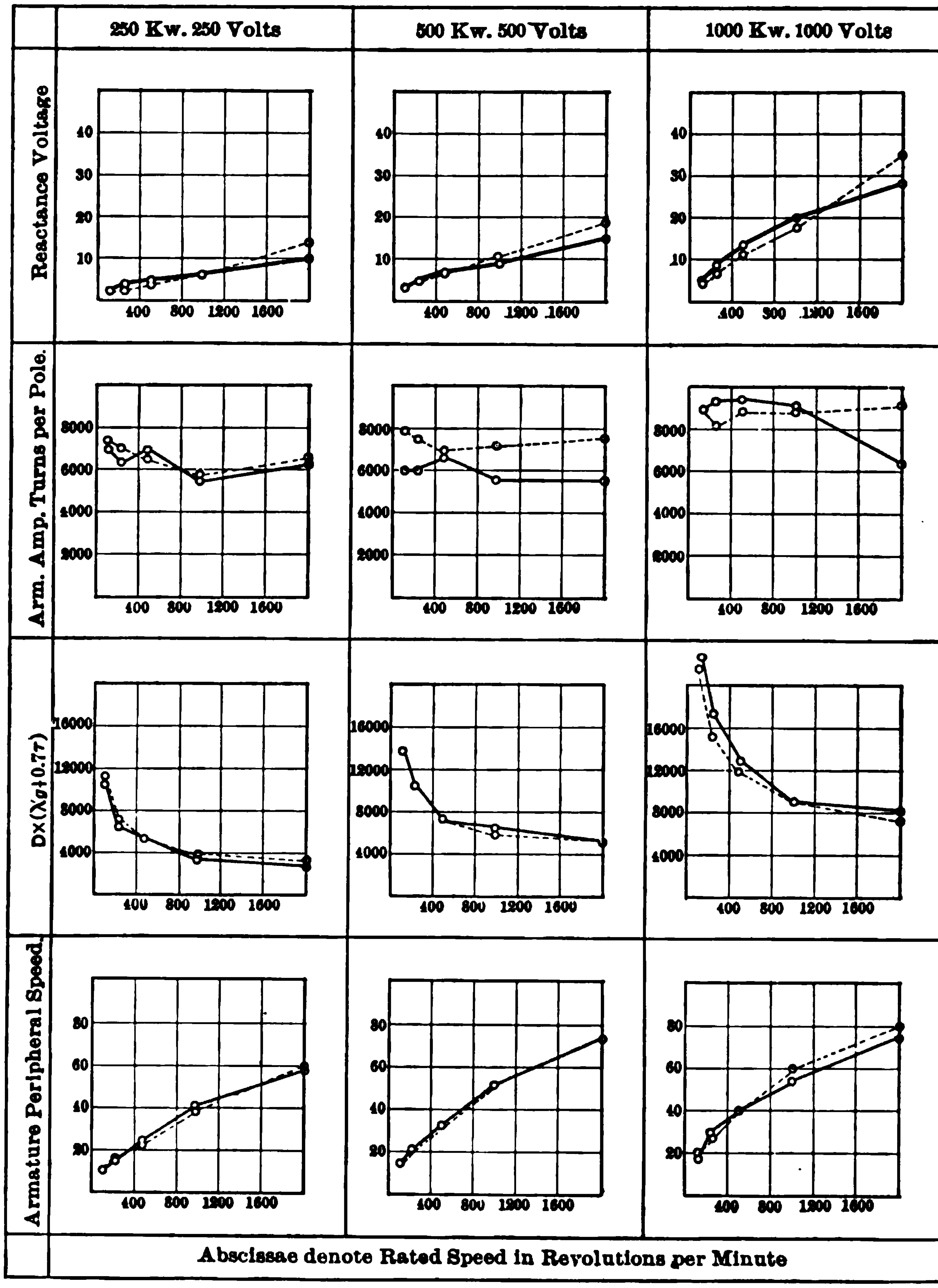


FIG. 277.— Curves showing in comparison the technical data obtained from the designs by chart method in Tables 55, 56, and 57, Chapter XIV, and designs given in specification in Table 58 of Chapter XV.

This comparison of results affords evidence of the usefulness of the method described in Chapter XIV as a ready means of arriving at a sound preliminary design for a continuous current dynamo.

Having thus checked and justified the preliminary method set forth in Chapter XIV, it is proposed to fall back on the data of the 45 designs, some of the leading constants of which have been set forth in Tables 55, 56, and 57, and to plot some of this data in groups of curves. This has been done in Figs. 278 to 286, relating respectively to:

Fig. No.	*Subject.*
278.	Reactance voltage. Volts per segment. Commutator peripheral speed.
279.	Total Works cost. Total weight.
280.	Armature ampere turns per pole. Flux per pole. Frequency in cycles per second.
281.	Heating constants. Armature watts per square decimetre. Commutator watts per square decimetre. Field spools watts per square decimetre.
282.	Commutator dimensions. Diameter at surface. Overall length. Width of segment + insulation.
283.	Commutator losses. I^2R losses. Friction losses. Total commutator losses.
284.	Armature losses. I^2R loss. Core loss. Total armature loss.
285.	Total losses of machine. Commutator losses. Armature losses. Field losses. Friction and windage losses. Total losses of machines.
286.	Efficiencies at full, half, and quarter load.

A great deal of valuable information is embodied in Figs. 278 to 286, and they will bear considerable study, which, however,

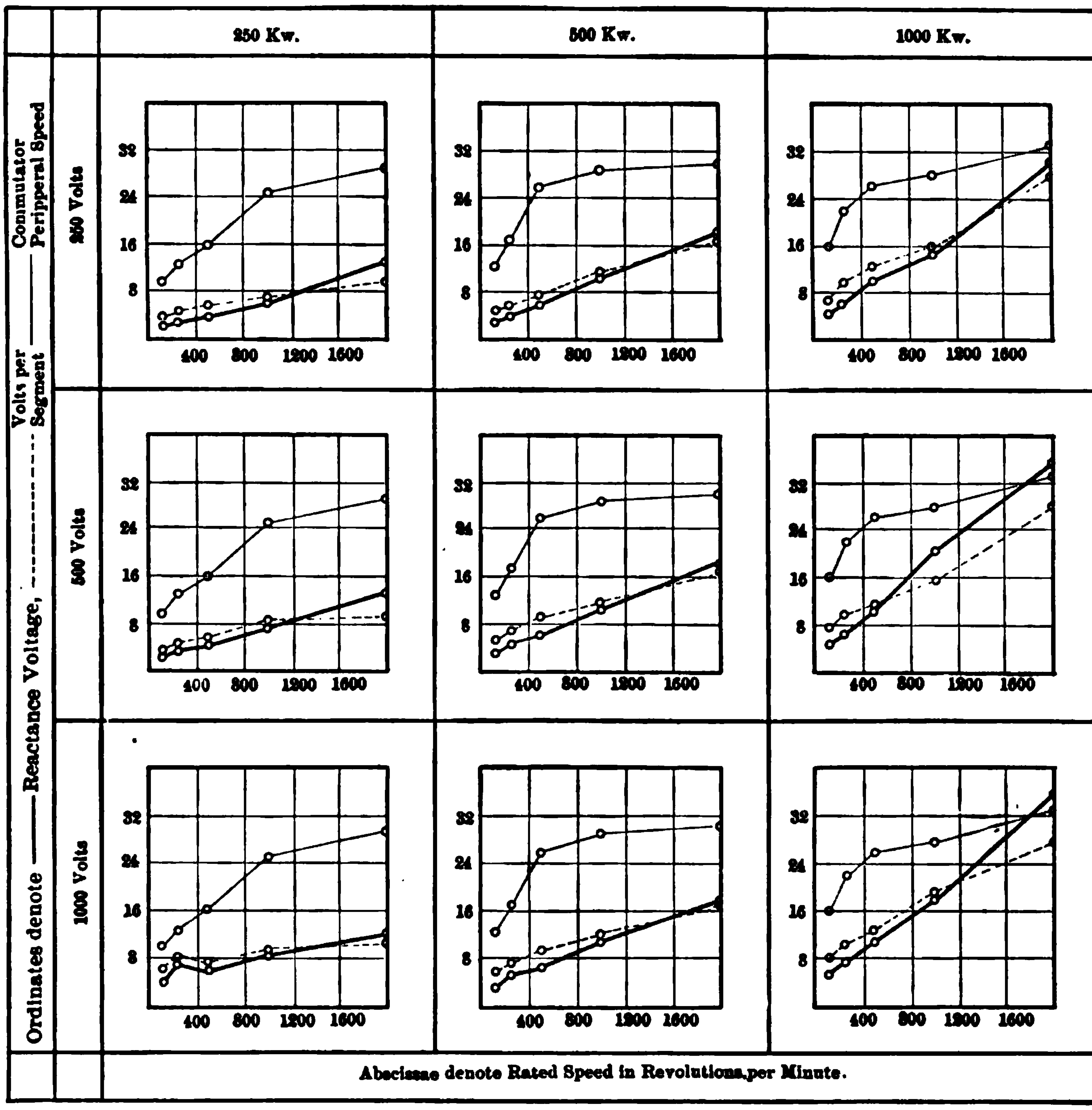


FIG. 278. — Curves showing values of reactance voltages, volts per segment, and commutator peripheral speeds for 45 preliminary designs for continuous current generators of various rated outputs voltages and speeds.

could not be rendered appreciably more profitable by anything which could be said in the text.

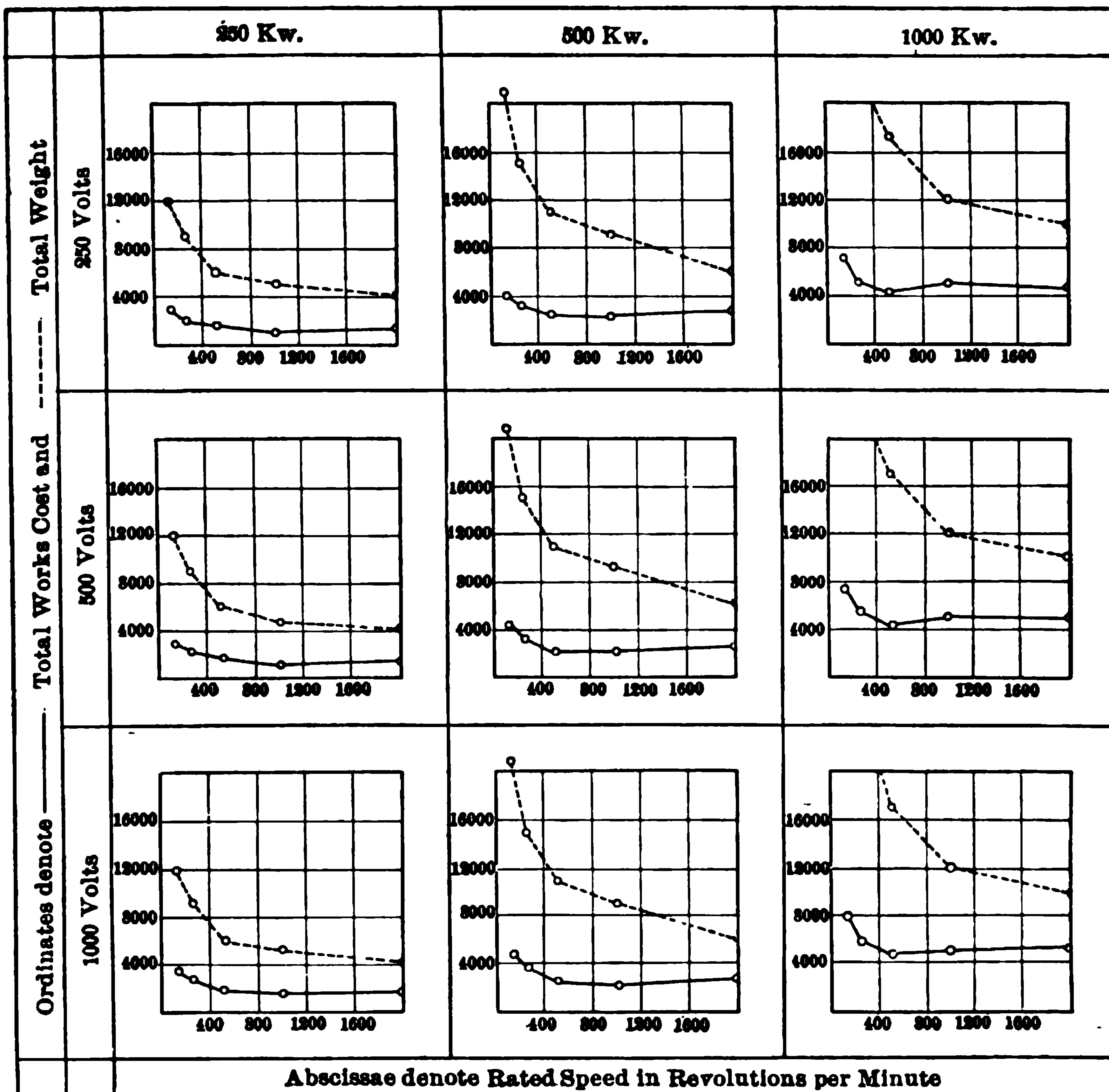


FIG. 279.—Curves showing values of Total Works Cost in dollars and total weight in kilograms for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

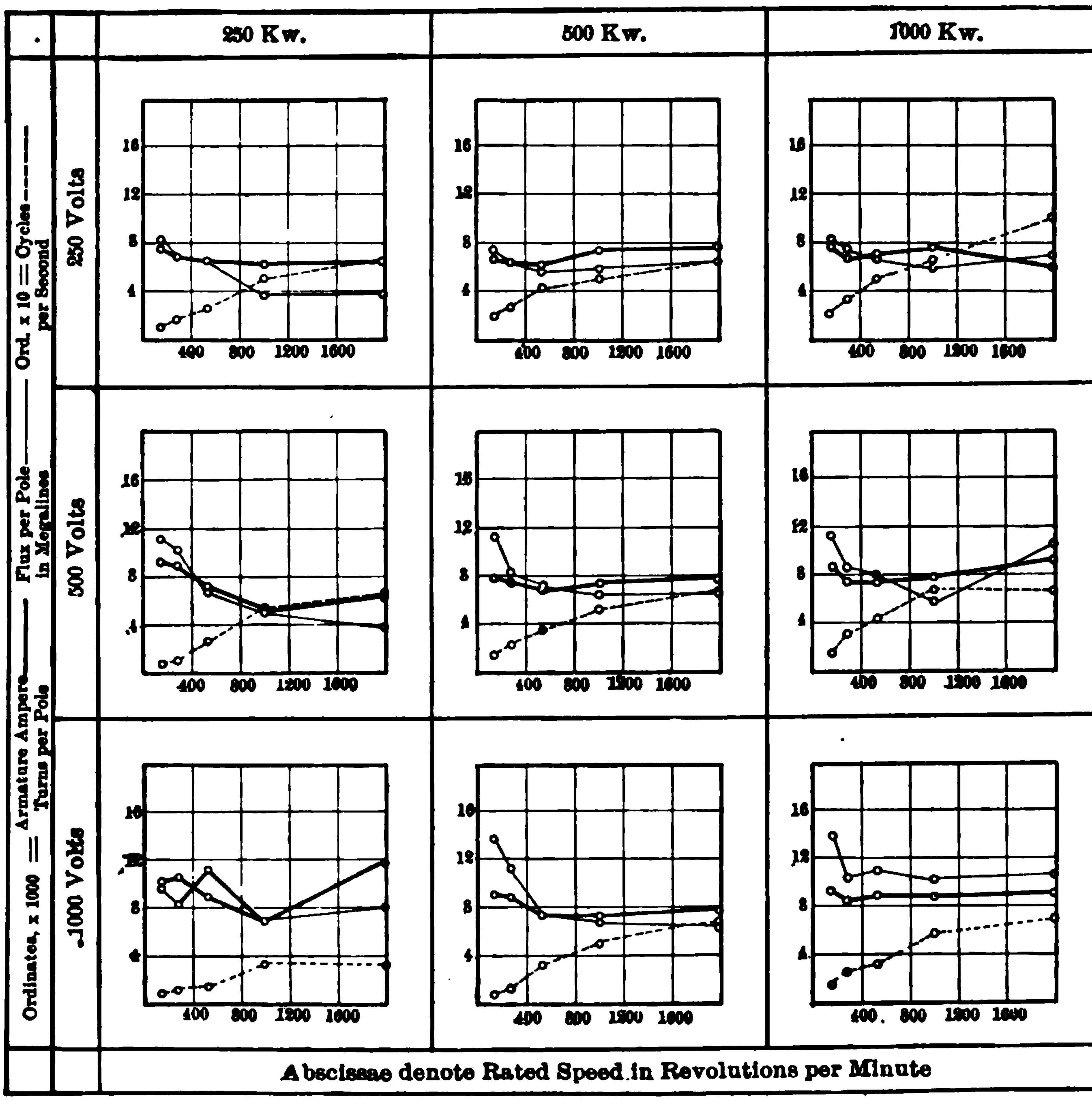


FIG. 280.—Curves showing values of armature ampere turns per pole, flux per pole in megalines and cycles per second for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

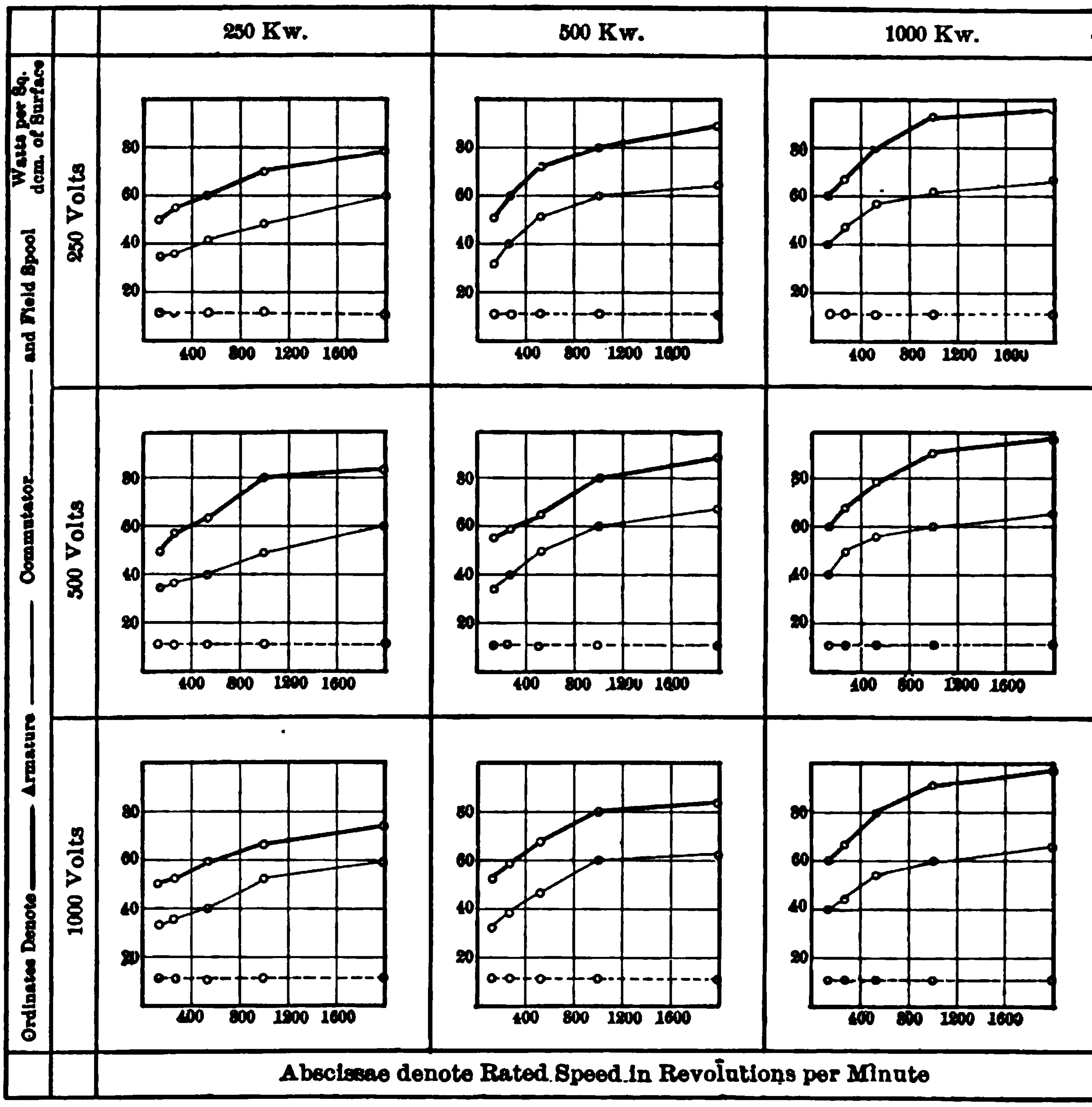


FIG. 281.— Curves showing heating constants expressed in watts per sq. dcm. of surface for armature, commutator and field spools for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

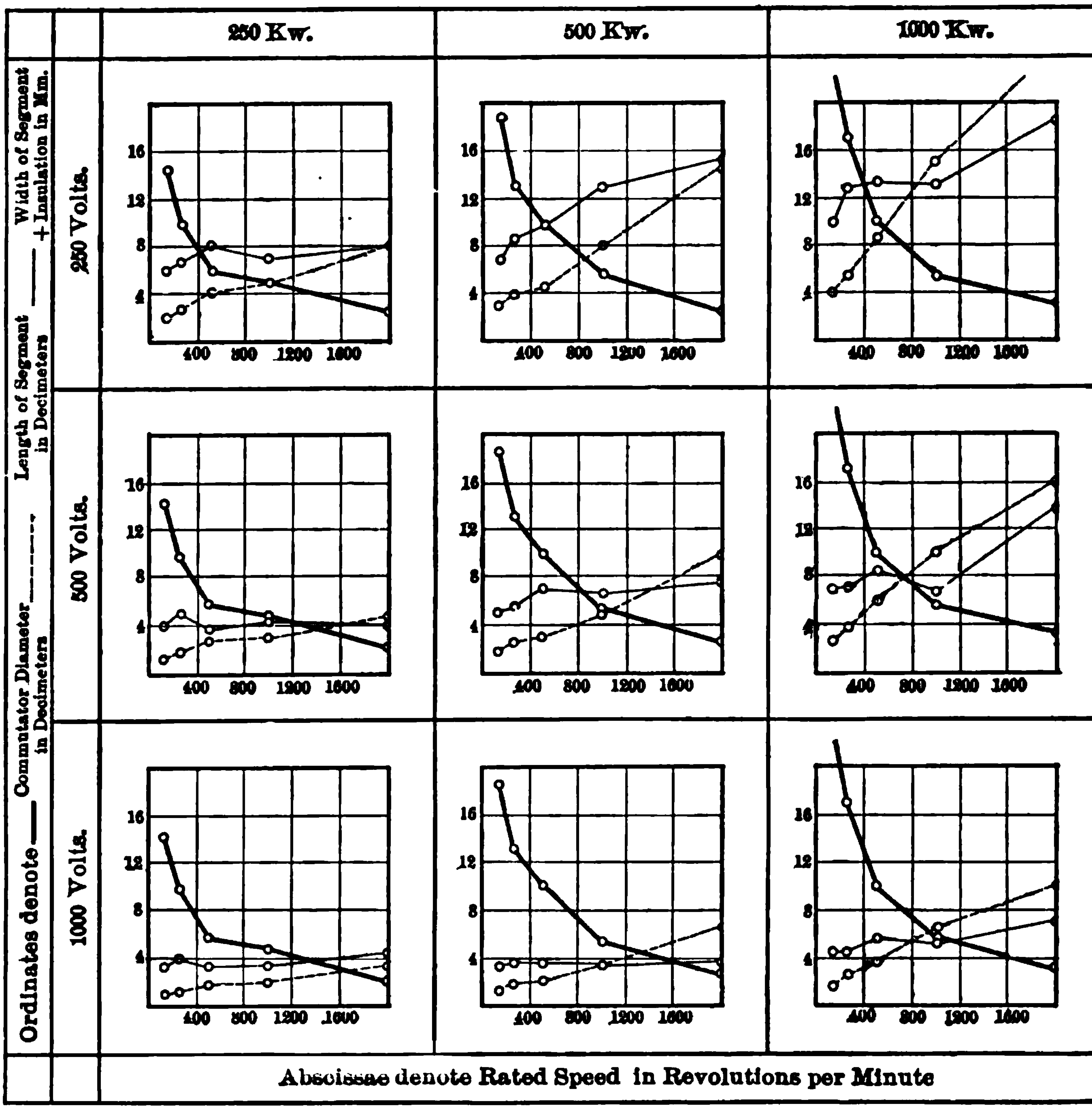


FIG. 282.— Curves showing commutator diameter, length of segment and width of segment insulation for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

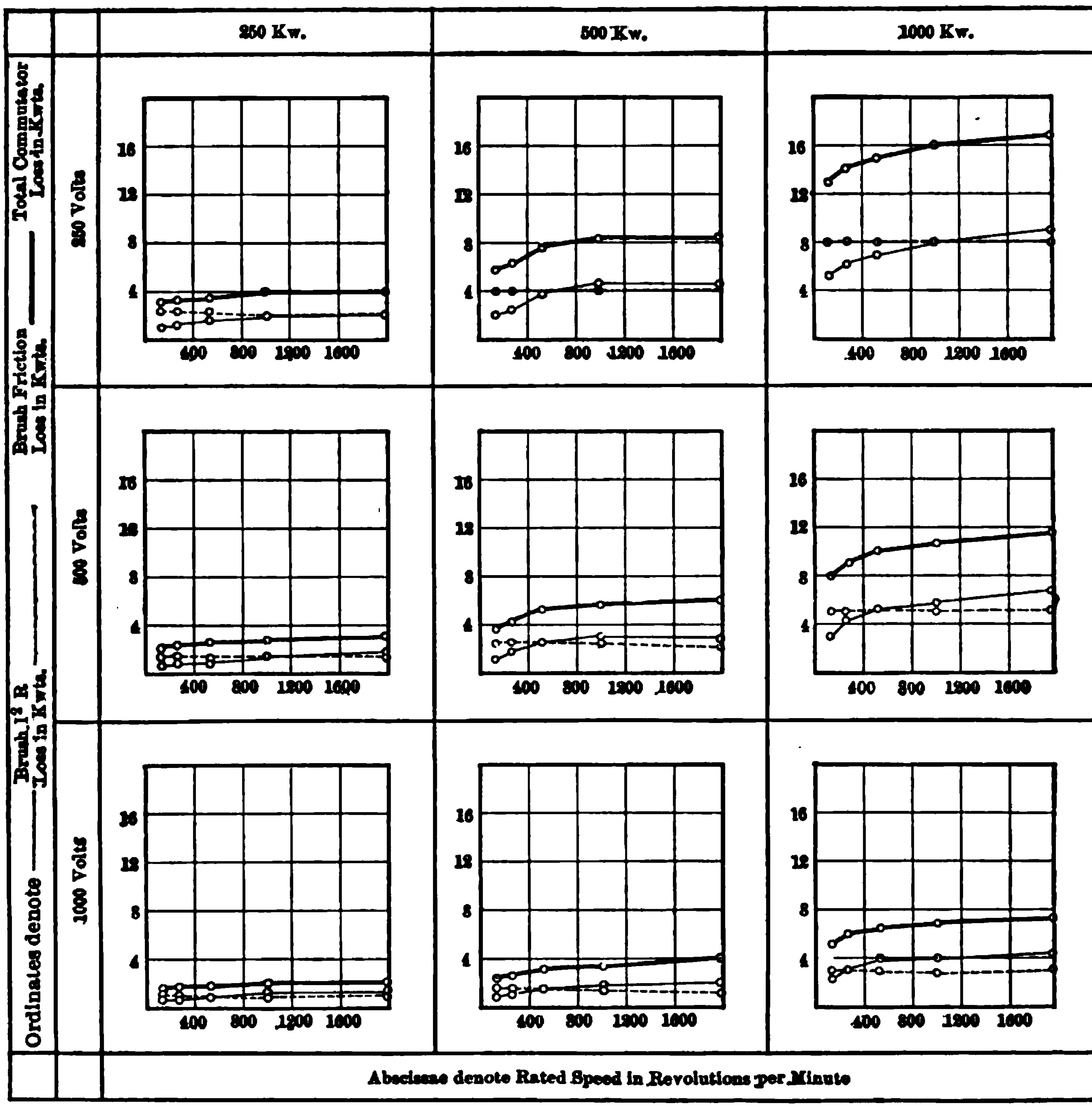


FIG. 283. — Curves showing values of brush I²R loss, brush friction loss and total commutator loss for 45 preliminary designs for continuous current dynamos of various rated outputs, voltages and speeds.

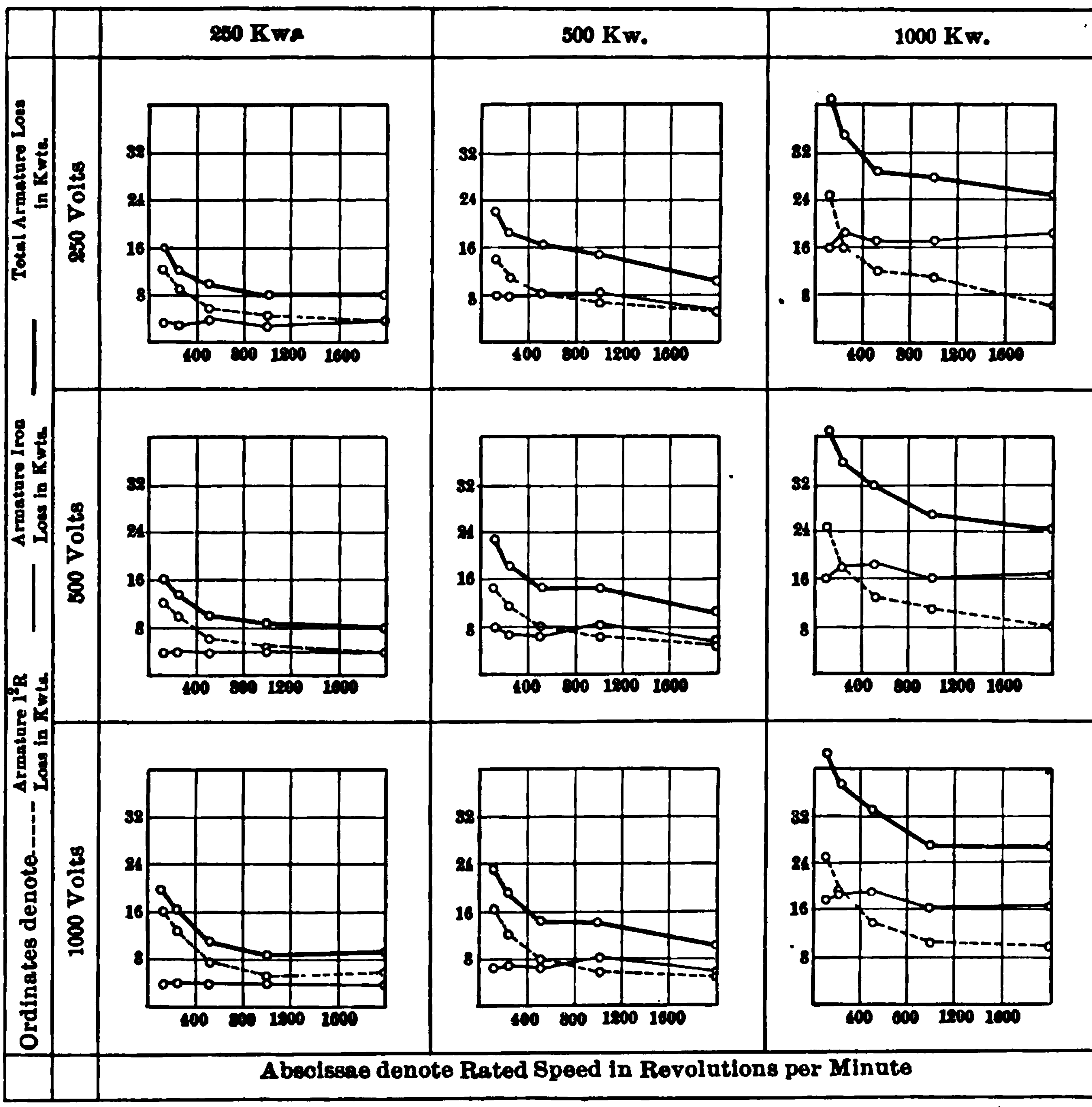


FIG. 284.— Curves showing values of armature $I^2\dot{R}$ loss, armature iron loss and total armature loss for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

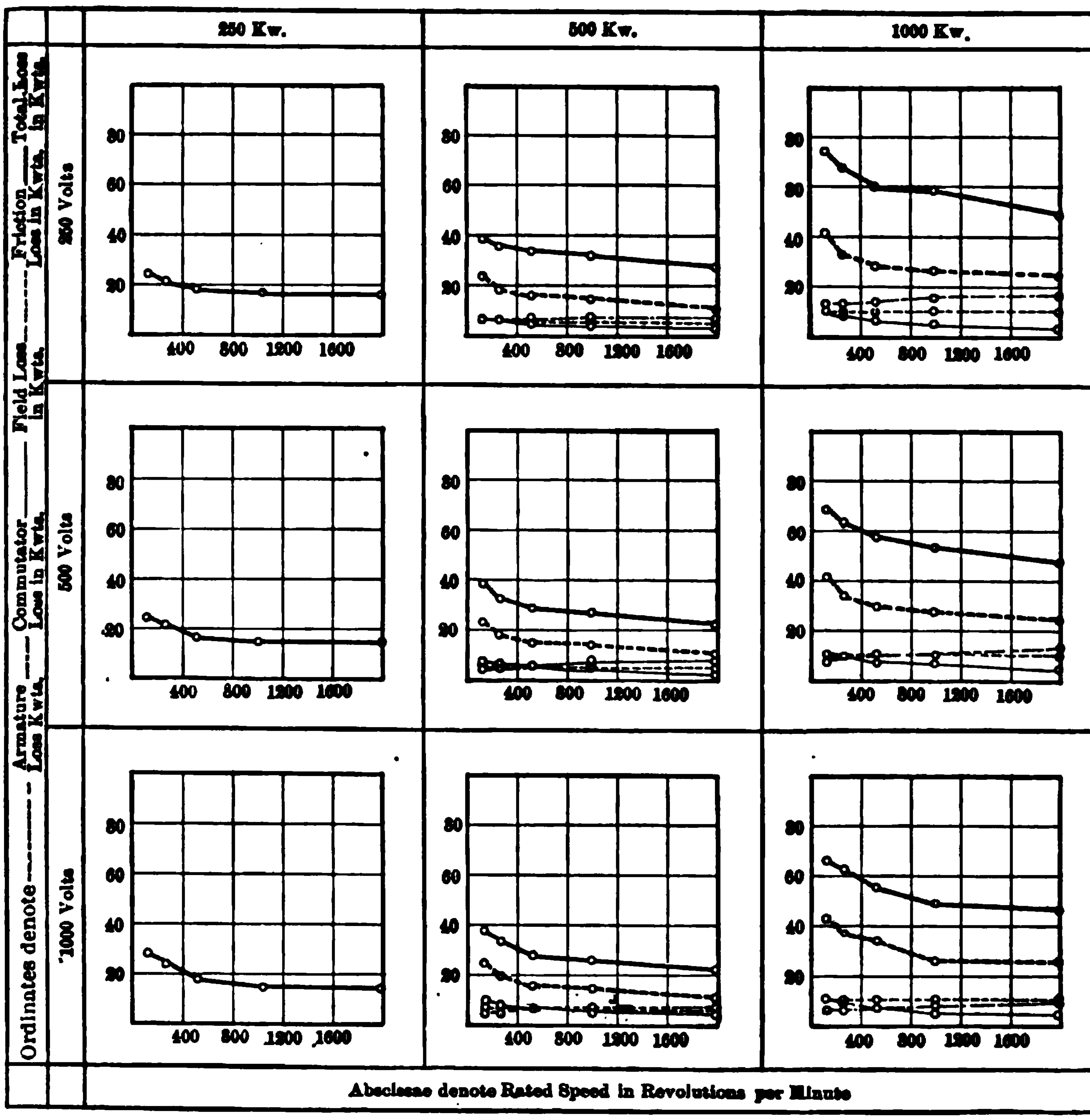


FIG. 285. — Curves showing values of armature loss, commutator loss, field loss, friction loss and total losses for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

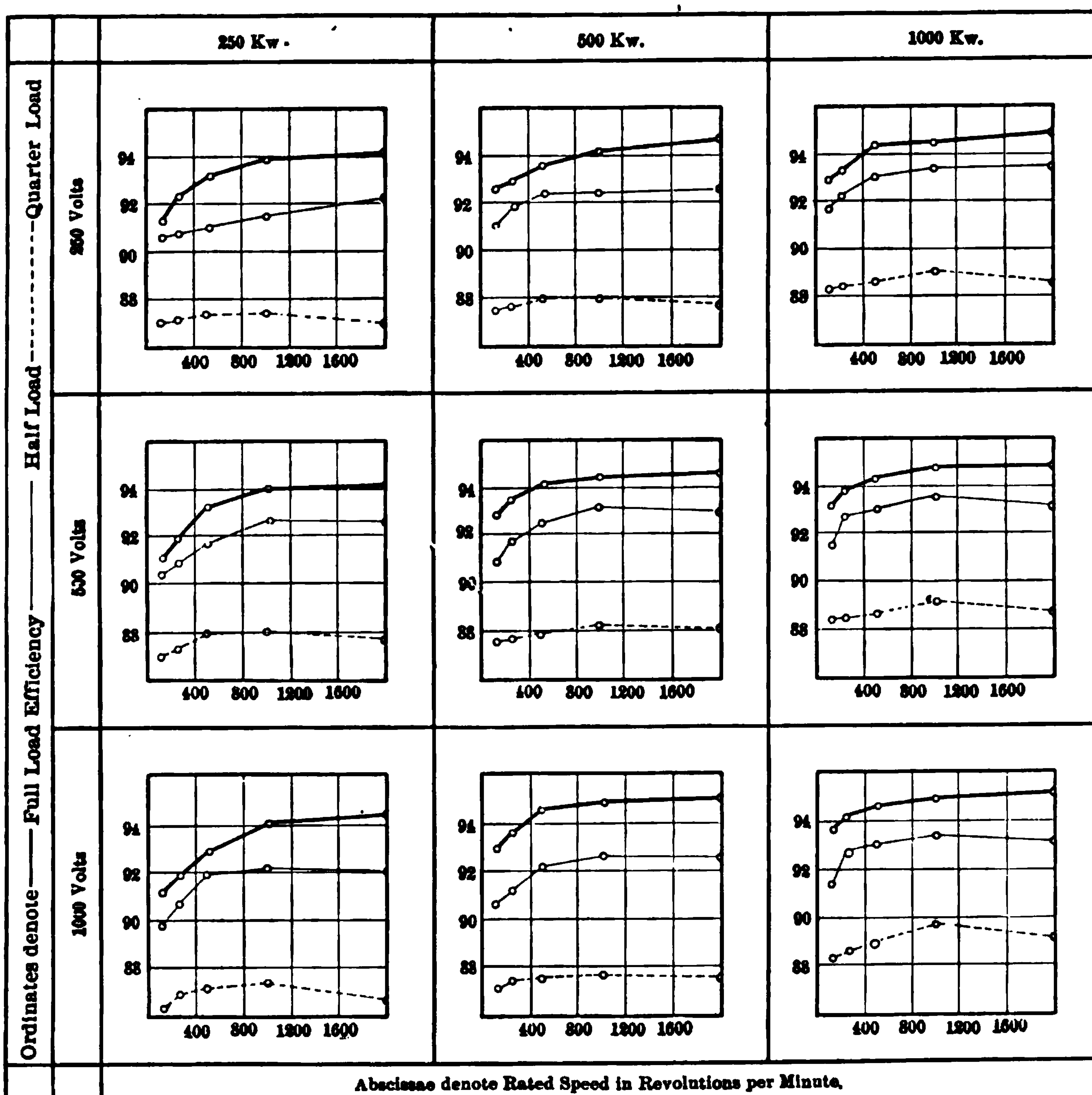


FIG. 286.— Curves showing values of full load, half load and quarter load efficiencies for 45 preliminary designs for continuous current generators of various rated outputs, voltages and speeds.

CHAPTER XVII.

TROUBLESOME RATINGS AND PROPOSALS FOR THEIR DESIGN.

In the preceding chapters we have been somewhat restrained in the preparation of the designs by the adherence to certain values of output coefficient, peripheral loading, etc. This is well enough for the purpose which we had in hand; it is, in fact, only by the elimination of certain variables that the remaining variables can be successfully handled in carrying out such extensive programmes. Moreover, for ordinary ratings, commercial considerations determine the magnitude of the permissible output coefficients, and sufficiently satisfactory results must be achieved by suitably balancing the various difficulties and without greatly exceeding the general range of outlay imposed by competition.

With very high speed ratings for large outputs, however, where no definite competitive basis has yet been established, the task may sometimes consist in obtaining a design with as good properties as engineering knowledge permits, and with nearly or complete independence of consideration of the outlay involved.

In continuous current dynamos designed for satisfactory operation without the use of interpoles, the reactance voltage should preferably not exceed 2.5 volts, and in most cases ought to be well below 2.0 volts. Hence, when designing interpole machines, the interpole must be so proportioned as to reduce the resultant voltage to this value.

The design of interpole machines is still in a somewhat crude stage owing to lack of operating experience and to diversity of opinion on various points: It is probable that interpoles cannot commercially be designed to neutralise the reactance voltage with a greater degree of exactness than some 10 per cent. Therefore machines having a reactance voltage of 20 volts and above, may when furnished with interpoles, still have a sufficiently large unneutralised reactance voltage to occasion sparking, especially with the high commutator

peripheral speeds which are unavoidable with high speed dynamos. It is with designs of this description that we propose to deal in this chapter.

For example, let us study the case of a 1000-kw. 1000-volt 1000 R.P.M. continuous current generator, the details of which were given in Table 58 of Chapter XV, and of which some of the leading constants are repeated in column A of Table 66 in the present chapter. A detailed drawing is shown in Fig. 264 on page 373. The reactance voltage of this machine is 20 volts; and although this value is occasionally exceeded in the design of high speed continuous current dynamos, it is very desirable that lower values should be obtained.

For a design of a given output and speed, in order to keep the reactance voltage to the lowest possible value, the armature diameter should be chosen as high as is consistent with mechanical strength, and the field should be made as strong as possible. To obtain these conditions a rectangular cross section of magnet core should be used, equal to the gross length of the armature and with the maximum practicable width at right angles to the shaft, thus obtaining the largest flux possible for a machine of stated dimensions. When this condition of strongest practicable field is obtained for a machine of given output coefficient and diameter, the reactance voltage can only be slightly decreased by any increase in λ_g.

In the formula —

$$\text{Reactance Voltage} = K \times R \times \lambda_g \times F \times \text{current per circuit},$$

the reactance voltage is proportioned to λ_g and to the number of face conductors.

Therefore, if we increase λ_g, together with the magnet core length parallel to the shaft, we must alter λ_g and F, in inverse proportion. Therefore the reactance voltage will remain practically the same, however much the length of the machine be increased, except for the slight decrease due to the change in the value of K.

In the 1000-kw. design under consideration, the armature peripheral speed was chosen sufficiently low to avoid undue mechanical stresses. Even at this diameter the reactance voltage could be reduced from 20 to 16 volts by using a magnet core of square cross section and cramping the field spools somewhat. This modification in the 1000-kw. 1000 R.P.M. design from Table 58 of Chapter XV

has been given in the second column of Table 66 (column B).

This is not an altogether satisfactory solution of the difficulty, as the closely packed windings (see the second design in Fig. 287) will lead to increased heating, and besides, magnet cores with square corners are seldom desirable.

In column C a design has been prepared to show the futility of making great increases in the axial length of the machine with the object of reducing the reactance voltage. In this design the armature diameter remains the same as for the designs in columns A and B, but the armature gross core length and the corresponding width of the magnet core parallel to the shaft are increased from 50 to 80 centimeters. Although the core has been increased by some 60 per cent in length, it has only been possible to reduce the reactance voltage from

FIG. 287. — Various designs for a 1000 kw. 1000 r.p.m. 1000 volt dynamo, with various reactance voltages.

16 to 15 volts, while the average volts per segment have risen to a very high value, namely, 37 volts. The Total Works Cost and the total weight are also considerably increased. The only way to substantially reduce this reactance voltage will be to increase the armature diameter, thus sacrificing the desirable factor of moderately low peripheral speed.

In columns D and E of Table 66 the two designs have been prepared with armature peripheral speeds of 68 and 89 metres per second, each design being worked out regardless of cost, but with the object of reducing the reactance voltage to as small a value as possible.

It has thus been reduced to a value as low as 9.0 volts in the last designs, but not without radical changes in some of the other constants.

The most important data consequent upon these changes have been selected from Table 66, and are shown below in Table 65.

TABLE 65.

LEADING DATA OF ALTERNATIVE DESIGNS FOR 1000 KW. 1000 VOLT 1000 R.P.M. CONTINUOUS CURRENT GENERATORS.

	A.	B.	C.	D.	E.
Reactance voltage	20	16	15	12	9.0
Average volts per segment	18	23	37	28	36
Armature peripheral speed	52	52	52	68	89
Full load efficiency	95.5	95.2	94.7	94.8	92.5
Total Works Cost	3800	4000	5200	6600	12,400

Although the reactance voltage of the design in column E has been reduced to 9 volts, the average volts per segment have risen to a very high value, and would certainly increase the tendency to "flashing" round the commutator. In consequence of the latter consideration, and also of the extremely high armature peripheral speed, this design appears the least practicable of the series, and it is also the most expensive. The most sound design is probably the one given in column D. The reactance voltage is reasonably low, and the Total Works Cost is not excessive. It is evident from the data given in Table 66 that for a machine of this output the rated speed of 1000 R.P.M. is too high for a really satisfactory machine.

In Chapter XVI it was shown that the limiting rated speed at which

it was thoroughly practical to obtain a genuinely satisfactory continuous current generator for this voltage, and rated output, is some 750 R.P.M.

TABLE 66.

ALTERNATIVE DESIGNS FOR 1000 KW., 1000 VOLTS, 1000 R.P.M. CONTINUOUS CURRENT GENERATORS.

	A.	B.	C.	D.	E.
GENERAL					
Rated output . . . . . kw.	1000	1000	1000	1000	1000
Voltage . . . . volts	1000	1000	1000	1000	1000
Speed . . . . R.P.M.	1000	1000	1000	1000	1000
Number of poles	6	6	6	6	8
ARMATURE					
Diameter (D) . . . . cms.	100	100	100	130	170
Gross length (λ_g) . . . cms.	50	50	80	40	40
Number of ducts	10	10	16	8	8
Net length (λn) . . . . cms.	36	36	57.5	29	29
Number of slots	162	132	162	212	224
Number of face conductors . .	648	528	324	424	448
Turns per segment	1	1	1	1	1
Peripheral speed . . Metres per second	52.5	52.5	52.5	68	89
COMMUTATOR					
Diameter of commutator . . .	58	58	58	60	70
Length of commutator . . .	50	50	50	50	45
Peripheral speed of commutator Metres per second	30	30	30	31.5	37
FIELD					
External diameter of yoke, cms.	200	200	200	240	290
Width of yoke . . . . cms.	80	100	117	70	70
Cross section of pole core . .	812	1030	1650	1260	1260
Shape of magnet core	Rect.	Rect.	Rect.	Circ.	Circ.
Length of air gap	0.7	0.7	0.7	0.7	0.7
WEIGHTS					
Weight of copper . . . . kgs.	1830	1800	1710	1600	1710
laminations . . kgs.	1200	1500	2400	2240	3300
cast steel . . . kgs.	1290	1600	2400	2150	2680
cast iron . . . kgs.	6000	7500	9100	8300	10,200
Total weight of effective material, tons	10.3	12.5	15.6	14.3	17.9
Total cost of effective material, dollars	.				
Total Works Cost	3800	4000	5200	6600	12,400
COEFFICIENTS					
Output coefficient	2.0	2.0	1.25	1.48	0.86
Ampere turns per cm. periphery	170	140	85	85	10.5
Armature ampere turns per pole	9000	7300	4500	5900	3550
Reactance voltage	20	16	15	12.3	9.2
Average volts per segment . .	18.5	23	37	28	36
Efficiency at full load	95.5%	95.2%	94.7%	94.8%	92.5%

Were it not for the fact that there is not sufficient room for the interpoles between the main poles, the design in column B of Table 66 would probably be the most satisfactory of the series. In order to further study the possibilities of designs for this rating, a modification of the interpole arrangement has been made. The main armature core of design A of Fig. 287 and Tables 65 and 66, has been extended at the end farthest from the commutator, and the interpoles are arranged over this extended core on an independent magnet system. This design is illustrated in Fig. 288, and the leading data are as follows:

General		
Rated output in kilowatts		1000
Voltage		1000
Speed	R.P.M.	1000
Number of poles		6
Armature		
External diameter (D)	cms.	100
Gross core length (λg)	cms.	50
Number of ducts		10
Net core length (λn)	cms.	36
Number of slots		204
Number of face conductors		408
Turns per segment		1
Peripheral speed	Metres per second.	52.5
Commutator		
Diameter		58
Length of segment		50
Peripheral speed	Metres per second.	30
Field		
External diameter of yoke		200
Width of yoke		80
Cross section of pole core	sq. cm.	1225
Shape of pole core section		Rectangular
Radial depth of air gap	cm.	0.7
Weights		
Copper	kgs.	1760
Laminations	kgs.	2200
Cast steel	kgs.	1850
Cast iron	kgs.	7500
Total weight of effective material	tons	13.3
Total cost of effective material	dollars	1800
Total Works Cost	dollars	5300

COEFFICIENTS

Output coefficient	1.5
Ampere turns per centimetre of periphery	110
Armature ampere turns per pole	5700
Reactance voltage	16.5
Average volts per segment	29
Efficiency at full load	94.5

Although in this design it appears that the reactance voltage must necessarily be increased by the greater core length, nevertheless as the interpoles are not crowded between the main poles it has been possible to gain the following advantages:

(1) Increased pole arc;

(2) Increased cross section of magnet core and depth of shunt winding;

(3) Decreased leakage factor.

Thus by taking advantage of these concessions it has been possible to rearrange the interpoles outside the main poles without appreciably increasing the reactance voltage.

The Total Works Cost has increased from $3800 to $5300, but this extra cost will be justified because of the improved features of the design, viz., reasonably low reactance voltage with an armature peripheral speed not exceeding 52 metres per second, average voltage per segment not excessive, being below 30 volts, and greatly increased facilities for heat dissipation, especially from the field coils.

In Fig. 289 a proposed method for the suppression of sparking at the commutator is shown. The commutator connections are extended to a sufficient length to permit of the introduction of a commutating pole or tooth. This plan is shown diagrammatically in the sketch on the right hand side of Fig. 289. At the moment before a short circuited segment leaves the brush, the commutator lead cuts a field of sufficient strength to reduce the current flowing into the brush. Therefore when the segment passes from under the brush, no current will be flowing in the commutator lead, and sparkless commutation will be obtained. The air gap between the pole A and the core must be as short as practicable in order to prevent fringing. This method is put forward merely on account of its interesting possibilities, no actual experiments having been made as to its practicability.

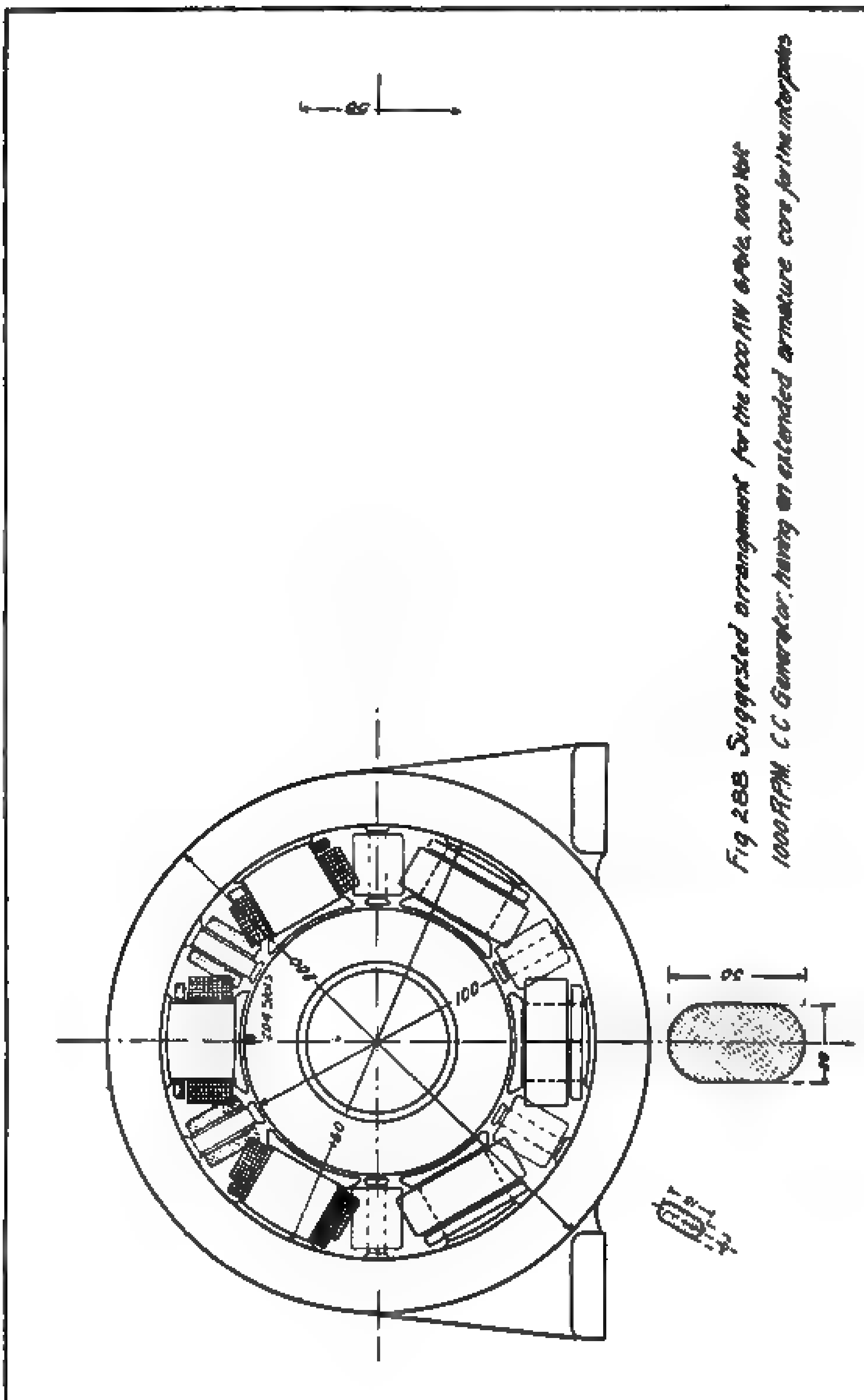


Fig 288 Suggested arrangement for the 1000 KW 6 Pole 1000 Volt 1000 RPM. C C Generator, having an extended armature core for the interpoles

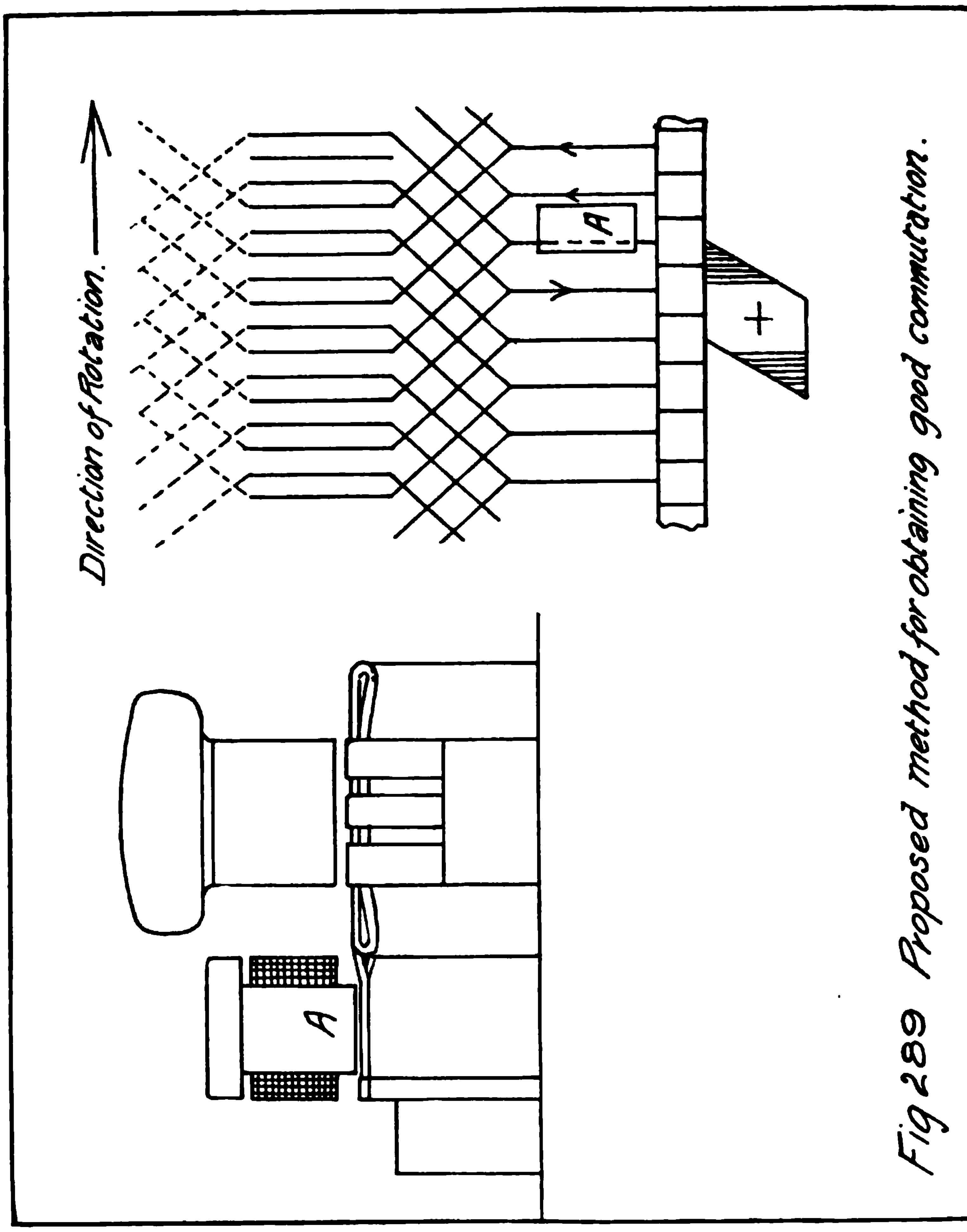


Fig 289 Proposed method for obtaining good commutation.

Rotary Converters as Substitutes for Speed Reduction Gearing. — Realising on the one hand the inevitably unsatisfactory nature of the design of continuous current generators of very large capacity for direct connection to steam turbines, and on the other hand the difficulties attending the reduction of the speed, one of the writers made, in the year 1902, a proposal which may be described as an electrical equivalent of a mechanical speed reduction. The following description of the proposal is illustrated by the diagram in Fig. 290. While, as we have seen, large continuous current generators for steam turbine speeds are at best very unsatisfactory, large alternators with stationary armatures and revolving fields may be designed very satisfactorily for these high speeds. Let us consider the case of a 1500 horse-power steam turbine running at 1500 R.P.M., from which we wish to obtain 1000 kw. of continuous current at 550 volts. The proposal, as applied to this case, would consist in generating 50-cycle alternating current from a 4-pole direct connected polyphase (say 6-phase) alternator with a stationary armature. The voltage per phase could be about 400 R.M.S. volts. The current is taken from the alternator to the six slip rings of a 1000-kw. shunt wound rotary converter, for a commutator pressure of 550 volts. The main current from the commutator of the rotary converter should — on its way to its load — excite the series coils of a small (say 20 kw.) exciter, the armature of which excites the revolving field of the main alternating current generator. By this combination, one obtains at will a 1000-kw. supply, either at constant voltage or over-compounded, for automatically increasing the voltage with the load.

As the rotary converter, used as here described, has no regulating function, it may be designed as cheaply as heating and commutation permit, and hence is free from the objections which exist with respect to the use of rotary converters in substations.

It is only by chance that 50 cycles is used in our example. It is used as the lowest periodicity consistent with the most satisfactory design of the alternator corresponding to 1500 R.P.M. and 4 poles — and the lowest periodicity is the most favourable condition for an economical and satisfactory design of rotary converter. For still larger capacity turbines and lower turbine speeds, correspondingly lower periodicities would be used. Moreover, there is in such a case no purpose in adhering to standard

periodicities, since the whole group of apparatus constitutes in the aggregate a *continuous current* generating set.

This system offers little if any advantage in lower cost of electrical apparatus, over slow speed generators, but it enables continuous current to be generated thoroughly satisfactorily from large high speed steam turbines, and hence it enables advantage to be taken of whatever superiority turbines possess over piston engines as regards first cost, economy, and floor space, except that so far as regards floor space, a part is sacrificed to the rotary converter.

Had a 2-pole 25-cycle alternating current generator been proposed in the preceding description, the design of the rotary converter would have been more satisfactory, but the alternating current generator would have been a much less satisfactory machine than can be designed with 4 poles for 50 cycles and the required output. It is a question of balancing the difficulties in the design of alternating current generator and rotary converter. For the former, 50 cycles is preferable for this output and speed; for the rotary converter, 25 cycles is to be preferred. On the whole, it seems preferable to employ 50 cycles for the system in this case.

A rough trial design has in the following pages been worked out for a 1000-kw. 4-pole 6-phase turbo-alternator for 50 cycles and 1500 R.P.M., and some of its regulation curves have been plotted.

From the curves of "excitation regulation" in Figs. 292 and 293, one sees that if the rotary converter's field adjustments are so made that the energy absorbed at its collector rings is always of power factor equal to 0.95 (leading), the excitation of the alternator may remain constant at 5000 ampere turns per spool, in which case constant voltage will be obtained at the rotary converter collector rings at all loads. It will be better to adjust the power factor at 0.90 and obtain a rising voltage to compensate for the IR drop — or even, if desirable, to *over* compensate and obtain continuous current from the commutator of the rotary converter at a voltage increasing at increasing loads.

These adjustments may, by means of shunt and series fields (and a diverter shunt around the series field) on the rotary converter, be readily arranged to be automatic. Thus the exciter may be dispensed with, the alternator being excited from the commutator of the rotary converter. A compound winding on the alternator would require a stationary field and rotating armature, which is, in general,

almost always an inferior arrangement, and especially so at steam turbine speeds, where the collection of large currents from slip rings should be avoided.

It may be thought that an exciter will be required at starting up. This is not the case. The rotary converter may be thrown on while the turbo-alternator is standing still, and will come up slowly in speed in synchronism with the turbo-alternator, and the fields of both machines may be built up from the remanent magnetism in their systems. If this should prove insufficient, there might be a mechanical or electromagnetic clutch between turbo-alternator and rotary converter, so that the latter should be mechanically run up part way or fully to its rated speed, and the clutch could then be released.

CALCULATIONS FOR CONTINUOUS CURRENT TURBO GENERATING SCHEME WITH ROTARY CONVERTER.

General Specification for Alternating Generator.

Number of phases	6 (two 3-phase)
Output . . . kw.	1000
Terminal voltage	400
Speed . . . R.P.M.	1500
Frequency per second	50
Number of poles	4

Armature Iron.

Diameter at air gap (D) . . . mm.	700
Diameter at bottom of slots	776
External diameter	926
Gross core length λg	800
Effective core length λn	600
Number of slots	72
Tooth pitch at armature face	30.6
Width of slot	17
Width of tooth at face	13.6
Depth of slot	38

Rotating Field.

Number of poles	4
Pole pitch (τ)	550
Depth of air gap	7.5
Diameter at pole face	685
Peripheral speed at pole face	55 metres per second
Depth of pole arc	280
Length of pole parallel to shaft	800
Breadth of pole across shaft	170

Armature Winding.—The armature winding is arranged as shown in Fig. 290, and consists in reality of two 3-phase delta-connected windings, giving six terminals. Each winding generates full voltage, 400, between its terminals, the current in each of the limbs A, B, C, etc., being one half of what the current per limb would be if the winding were a single 3-phase delta-connected winding.

For a single 3-phase delta connected machine the current would be $\frac{1,000,000}{400 \times 3} = 832$ amperes. Hence the current per limb for the

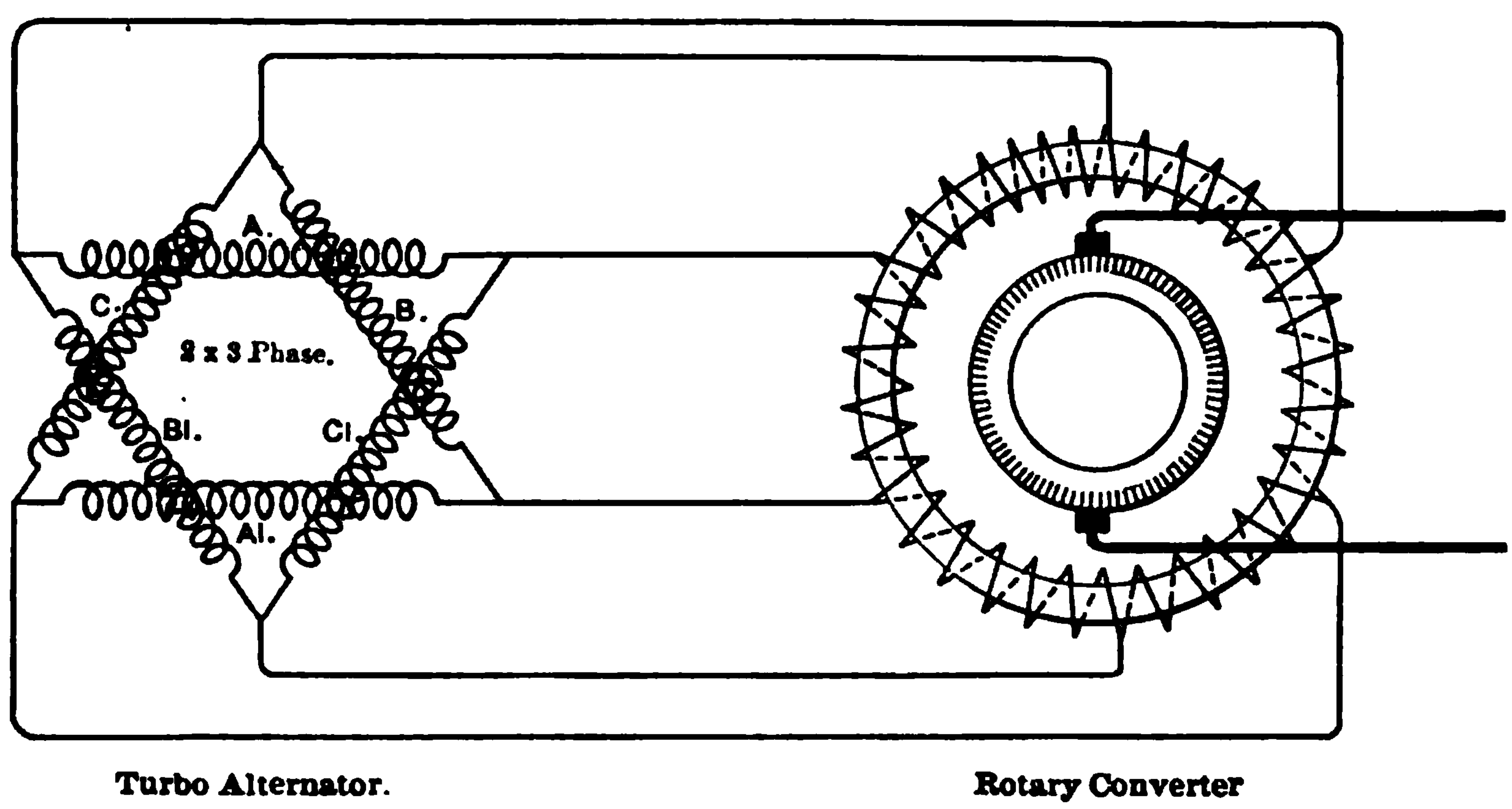


Fig. 290.— Diagrammatic 2-pole representation of rotary converter scheme for a continuous current turbo-generator.

6-phase arrangement is 416 amperes. The total number of slots is 72 (8 per pole). There are four conductors per slot, arranged two by two, the upper pair being in parallel, and belonging to, say, phase A (Fig. 290), the lower pair, in parallel, belonging to phase A' (Fig. 290).

Amperes per phase	416
Amperes per conductor	208
Dimensions of single conductor — mm.	15 × 6
Section of single conductor — sq. cm.	0.9
Current density amps. per sq. cm.	231

For the purpose of calculating the armature demagnetising ampere turns, consider the armature as a 3-phase winding, each phase carrying 832 amperes. Then we have

Total number of armature conductors = 4×72 =	288
Number of conductors per phase	96
Number of conductors per pole and per phase	24
Number of conductors in series per pole and per phase	6
Number of turns in series per pole and per phase	3
Armature R.M.S. ampere turns per pole and per phase 3×832 =	2496
Resultant ampere turns per pole for three phases = $2496 \times 2\sqrt{2}$ =	7100

The magnetic circuit requires 5000 ampere turns per pole at no load for 400 volts.

We will now proceed to calculate the excitation regulation curves for full load, half load, and quarter load at various power factors.* The armature inductance may be estimated at 0.0009 henry per phase, whence the reactance is

$$2\pi \times 50 \times 0.0009 = 0.283 \text{ ohm.}$$

At full load current the reactance voltage is

$$0.283 \times 416 = 115 \text{ volts.}$$

The interaction of the other phases generally increases this somewhat, so we may take 130 volts at full load.

Fig. 291 is a vector diagram representing the conditions. In this diagram: —

OC is the current.

ϕ the angle corresponding to the power factor of the external circuit.

OV, the terminal voltage, = 400.

OR the reactance voltage.

For full load current of 416 amperes per phase (6-phase) we have $OR = 130$ volts.

$OV = 400$ volts set out at an angle ϕ with the current OC.

θ is the angle of displacement of maximum current from the mid pole face position, and the armature demagnetisation for any power factor is equal to the armature ampere turns per pole $\times \sin\theta$, and for full load $= 7100 \sin\theta$.

The no load field ampere turns added to the demagnetising ampere turns give approximately the field ampere turns required for any

* The method employed in this instance is discussed in "Electric Machine Design" by H. F. Parshall and H. M. Hobart, London, 1905.

given conditions as to load and power factor. In this way we have calculated the excitation required at various power factors from zero to unity for leading and lagging currents, firstly for full load, secondly

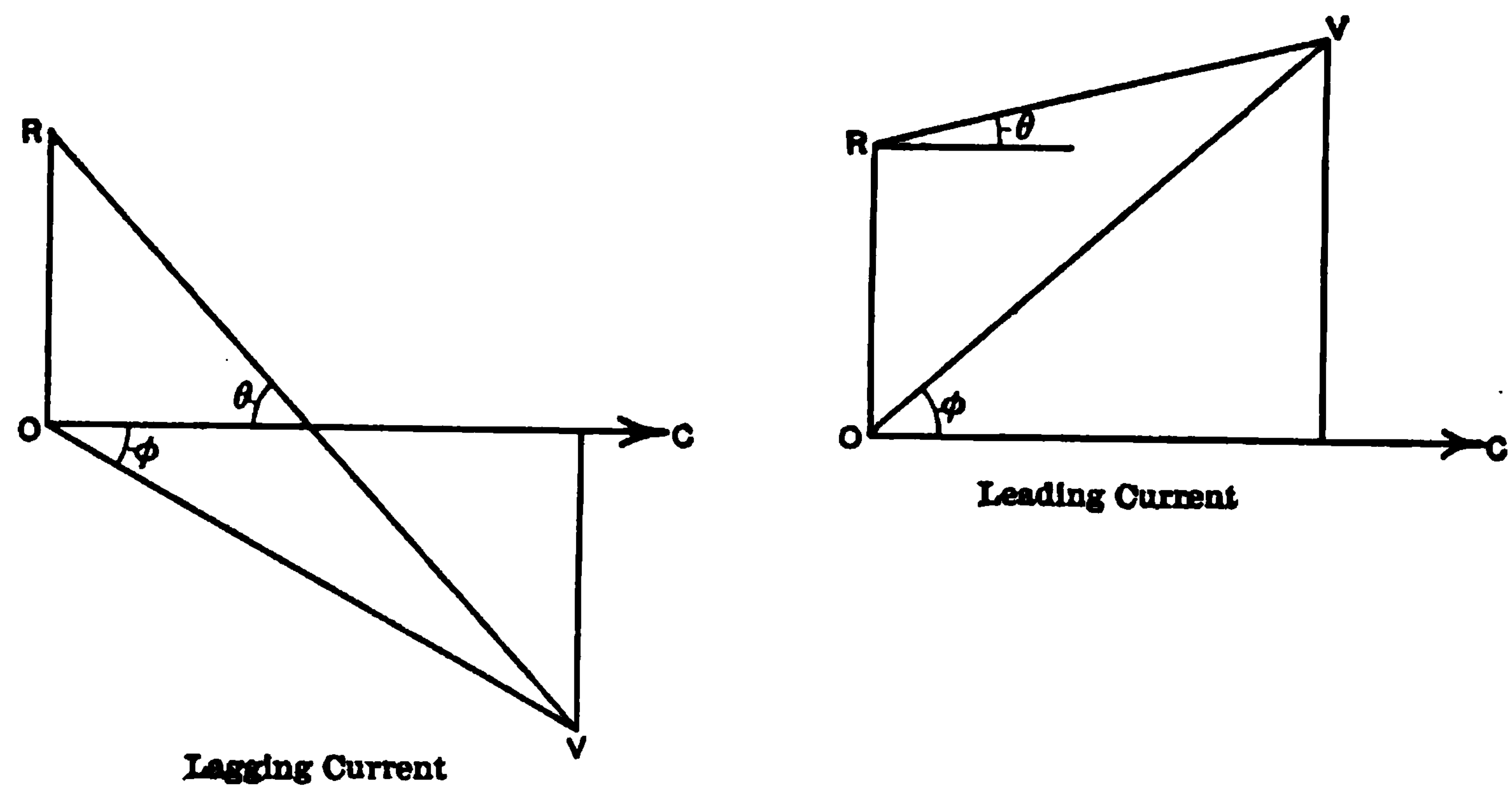


FIG. 291.— Vector diagram for alternating current generator.

for one half load, and thirdly for one quarter load. These results are embodied in Tables 67, 68, and 69, and from them we have plotted

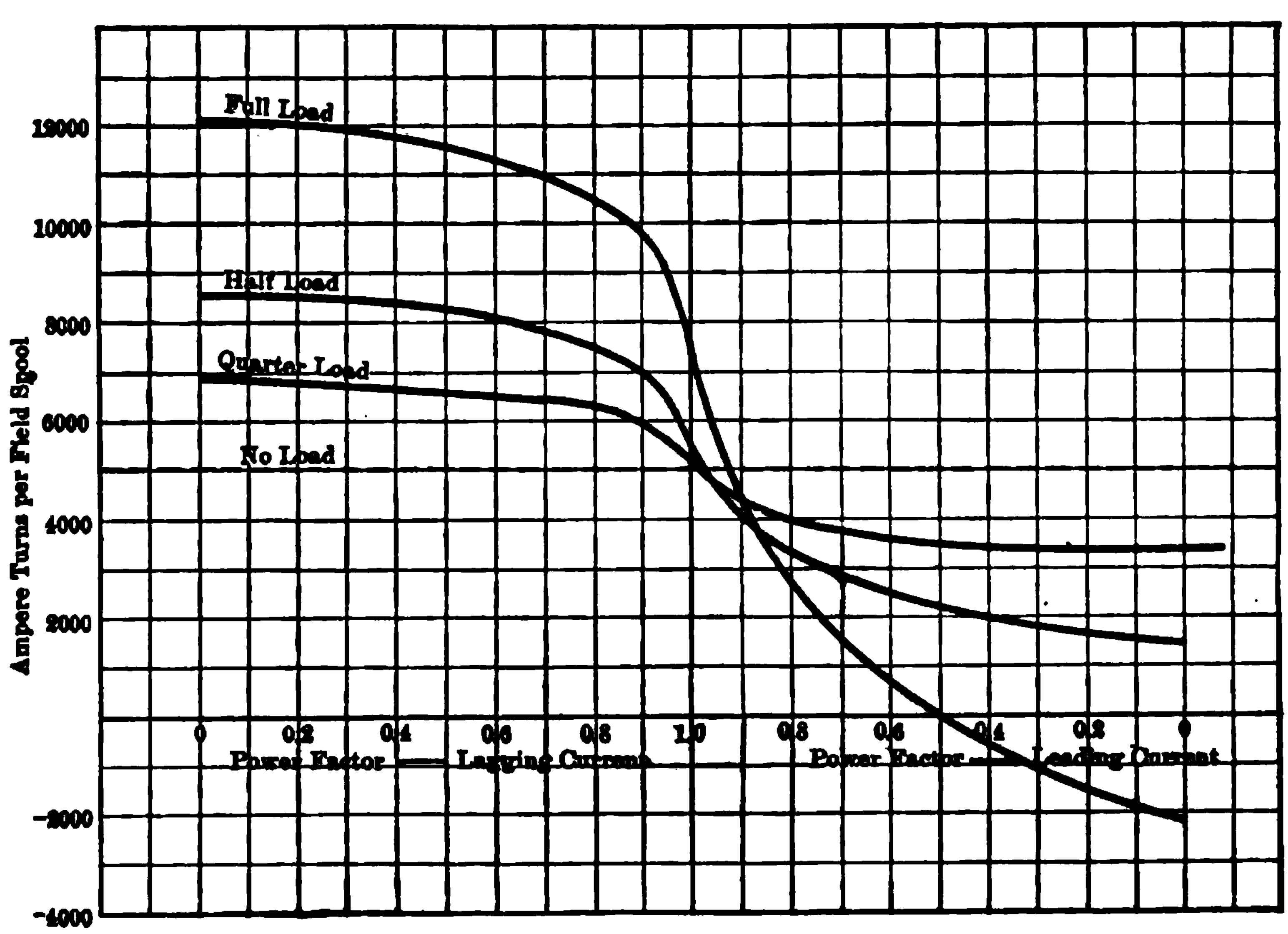


FIG. 292.— Excitation regulation curves for 1000 k.w. alternator.

in Fig. 292 curves showing the relation between excitation required for 400 terminal volts at full, one half, and one quarter full load current, and the power factor.

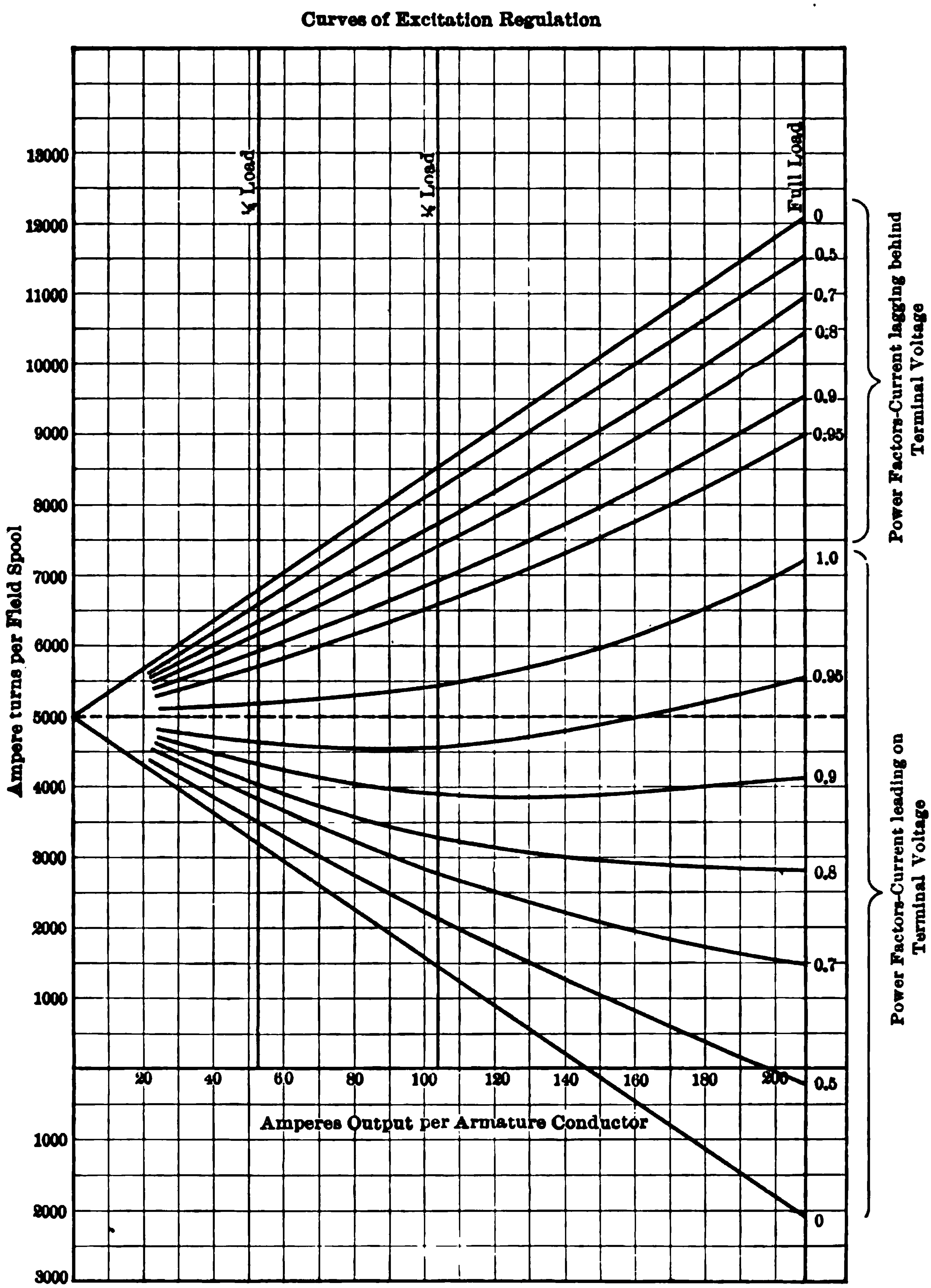


FIG. 293. — Excitation regulation curves for 1000 kw. alternator.

From these curves we have plotted in Fig. 293 a set of curves for various power factors, connecting amperes output and excitation for 400 terminal volts.

From the curves in Fig. 292 it is apparent that if we have a load of power factor 0.95 with leading current, the alternator will regulate for constant terminal voltage at all loads with constant excitation of 5000 ampere turns per field spool.

In the case that the entire load consists of a rotary converter whose field excitation one adjusts once for all by suitable shunt and series windings to make the current input leading and of approximately 0.95 power factor for all loads, then the excitation of this alternating current generator may remain constant at about 5000 ampere turns per spool, and the system will automatically maintain 400 volts per phase at all loads.

As the object in view would generally be to obtain a pressure increasing slightly and automatically with the load, the rotary converter's spool windings would generally be adjusted for a slightly lower power factor at all loads, say 0.90, or a power factor decreasing from 0.95 at light loads, to, say, 0.85 at full load.

Thus, with a suitable rotary converter the terminal voltage of this alternator would *increase* in proportion to the load, the combined set giving out continuous current at a constant terminal voltage at all loads, or if necessary a voltage increasing with the load.

TABLE 67.

ESTIMATION OF "EXCITATION REGULATION" CURVES FOR 400 TERMINAL VOLTS AND FULL LOAD CURRENT (=208 AMPERES PER CONDUCTOR).

Power Factor (cos ϕ).	Phase of Current with Respect to Terminal Voltage.	Amperes per Conductor.	Reactance Voltage.	Sin ϕ.	Sin θ.	Armature Reaction in Ampere Turns.	Phase of Current with Respect to Internal Voltage.	Required Excitation for 400 Terminal Volts.
0.00	Lag	208	130	1.00	1.00	7100	Lag	12,100
0.50	"	"	"	0.866	0.925	6550	"	11,550
0.70	"	"	"	0.72	0.83	5900	"	10,900
0.80	"	"	"	0.60	0.76	5400	"	10,400
0.90	"	"	"	0.435	0.64	4550	"	9,550
0.95	"	"	"	0.320	0.56	4000	"	9,000
1.00	...	"	"	0.00	0.31	2200	"	7,200
0.90	Lead	"	"	0.435	0.12	850	Lead	4,150
0.80	"	"	"	0.60	0.32	2300	"	2,700
0.70	"	"	"	0.72	0.49	3500	"	1,500
0.50	"	"	"	0.866	0.74	5250	"	250
	"	"	"	1.00	1.00	7100	"	2,100

TABLE 68.

ESTIMATION OF "EXCITATION REGULATION" CURVES FOR 400 TERMINAL VOLTS AND HALF LOAD CURRENT (=104 AMPERES PER CONDUCTOR).

Power Factor (cos ϕ).	Phase of Current with Respect to Terminal Voltage.	Amperes per Conductor.	Reactance Voltage.	Sin ϕ.	Sin θ.	Armature Reaction in Ampere Turns.	Phase of Current with Respect to Internal Voltage.	Required Excitation for 400 Terminal Volts.
0.00	Lag	104	65	1.00	1.00	3550	Lag	8550
0.50	"	"	"	0.866	0.90	3200	"	8200
0.70	"	"	"	0.72	0.78	2750	"	7750
0.80	"	"	"	0.60	0.69	2450	"	7450
0.90	"	"	"	0.435	0.55	1950	"	6950
0.95	"	"	"	0.320	0.45	1600	"	6600
1.00		"	"	0.00	0.161	470	...	5470
0.90	Lead	"	"	0.435	0.29	1050	Lead	3950
0.80	"	"	"	0.60	0.48	1700	"	3300
0.70	"	"	"	0.72	0.62	2200	"	2800
0.50	"	"	"	0.866	0.81	2850	"	2150
0.00	"	"	"	1.00	1.00	3550	"	1450

TABLE 69.

ESTIMATION OF "EXCITATION REGULATION" CURVES FOR 400 TERMINAL VOLTS AND QUARTER LOAD CURRENT (=52 AMPERES PER CONDUCTOR).

Power Factor (cos ϕ).	Phase of Current with Respect to Terminal Voltage.	Amperes per Conductor.	Reactance Voltage.	Sin ϕ.	Sin θ.	Armature Reaction in Ampere Turns.	Phase of Current with Respect to Internal Voltage.	Required Excitation for 400 Terminal Volts.
0.00	Lag	52	325	1.00	1.00	1780	Lag	6780
0.50	"	"	"	0.866	0.89	1580	"	6580
0.70	"	"	"	0.72	0.75	1330	"	6330
0.80	"	"	"	0.60	0.66	1170	"	6170
0.90	"	"	"	0.435	0.50	890	"	5890
0.95	"	"	"	0.320	0.39	695	"	5695
1.00	...	"	"	0.00	0.08	142	"	5142
0.90	Lead	"	"	0.435	0.36	640	Lead	4360
0.80	"	"	"	0.60	0.54	960	"	4040
0.70	"	"	"	0.72	0.67	1200	"	3800
0.50	"	"	"	0.866	0.84	1500	"	3500
0.00	"	"	"	1.00	1.00	1780	"	3220

CHAPTER XVIII.

CONSTRUCTION OF HIGH SPEED CONTINUOUS CURRENT GENERATORS.

AMONGST the examples of *alternating current* generators given in Chapter XI some designs of fairly presentable appearance are to be found. *Continuous current* turbo-generators are, however, of much less satisfying appearance. In designs of small capacity this is a consequence not only of the disproportionate commutator dimensions, but also of the abnormal dimensions which must be employed for the armature and field design. In machines of large capacity, the unsatisfactory appearance relates chiefly to the commutator and to the collecting gear, and is consequently more accentuated, for a given rated output and speed, the lower the voltage.

In Fig. 294 is reproduced the outline design for a 1000-kw. 1000 R.P.M. continuous current generator for a pressure of 1000 volts. Even at this — for continuous current practice — still unusual pressure, the length of the commutator interferes with the otherwise fairly attractive appearance of the design. In Fig. 295 are given, one above the other, a set of outline sketches for this same output and speed, and for pressures of 2000, 1000, 500, 250, and 125 volts. These are all proportioned for carbon brushes, and for a heating coefficient of 70 watts per square decimetre of commutator surface.

It is this state of affairs which, in addition to the friction difficulties attending the operation of carbon brushes on commutators with high peripheral speeds, has led manufacturers of continuous current turbo-dynamos to employ either compound brushes consisting of copper or other high conductivity material, for collecting the main current and pilot brushes of high resistance for dealing with the commutation, or else graphite brushes permitting of fairly high current densities. In the former case, and assuming that freedom from sparking is secured, the total commutator loss may be reduced to a small proportion — say 25 per cent — of the amount involved when ordinary carbon brushes are employed. In the latter case

FIG. 294. — General arrangement of high speed continuous current generator.— 1000 kw. 6-pole 1000 r.p.m.

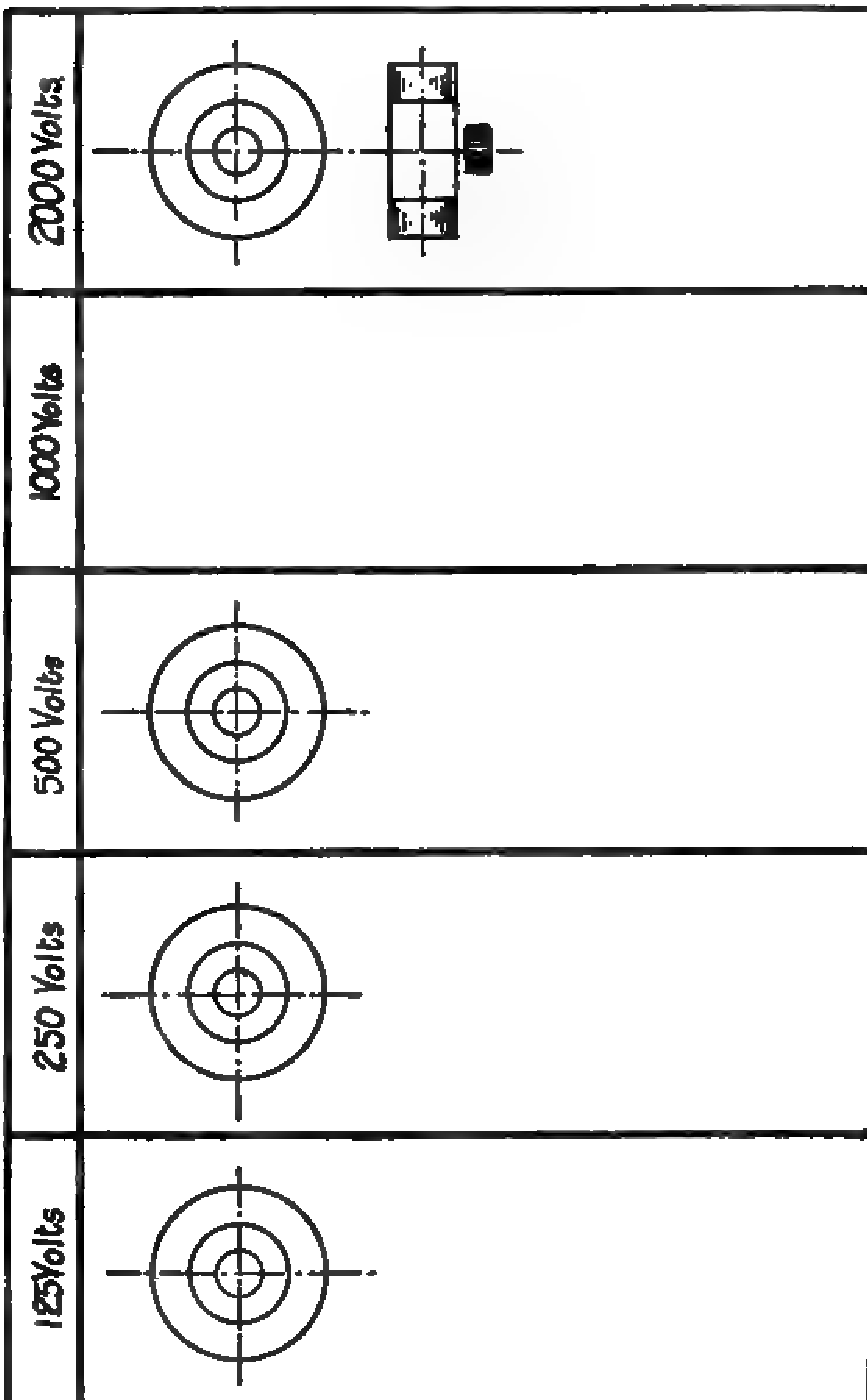


FIG. 295. — Outline sketches of 1000 kw. 1000 r.p.m. continuous current generator with commutator dimensions proportioned for various voltages employing ordinary carbon brushes.

(i.e., that in which graphite, or other intermediate grade brushes are employed), there is a fair amount of evidence accumulating, to justify estimating on a total loss of some such value as half that incurred with ordinary carbon brushes.

Since, however, the ventilating difficulties with long commutators of small diameter are decidedly serious, so high a specific loss as 70 watts per square decimetre of surface is generally associated with a prohibitive, or at any rate a very undesirably high temperature rise. Hence in high voltage machines it is desirable to take up any advantage accruing from brush improvements, in improved operation at lower temperature rise. At lower voltages, however, the length of commutator must be decreased at almost any sacrifice.

Thus for the set of machines in Fig. 295 the use of improved brushes should not, in the 2000-volt design, be accompanied by any modification of the commutator dimensions. In the 1000-volt design, it is justifiable to decrease the length by 10 per cent; in the 500-volt design by 20 per cent, and in the 250-volt design by 40 per cent. A 125-volt design for 1000 kw. at 1000 R.P.M. should, with our present knowledge of the subject, not be built.

These revised designs are indicated for 2000, 1000, 500, and 250 volts in Fig. 296. The 2000 and 1000-volt designs are now fairly presentable, and the 500-volt design is passable, but the 250-volt design is still distinctly hideous.

Should we undertake similar comparisons as regards somewhat larger rated outputs at this same speed, the 250-volt rating would first have to be discarded, and at still larger rated outputs, the 500-volt rating. Thus it is only for very small rated outputs that continuous current generators for steam turbine speeds are even remotely sound propositions. It should not be necessary to reiterate our recommendation to insist, at all costs, on reduced speeds for steam turbines when supplied for such ill advised purposes as the driving of continuous current generators. The desirability of reducing this speed is the more imperative, the lower the required pressure. Our experience, however, tells us that but very few engineers will profit by this advice. The great majority will perversely insist on employing steam turbine drive for precisely this class of work.

Now the reduction in speed increases the steam consumption of a turbo-generator. Hence to employ steam turbine drive in a continuous current generating station involves the use of many units

each of small rated capacity, and these are uneconomical units even for that capacity. The lack of economy is still greater when compared

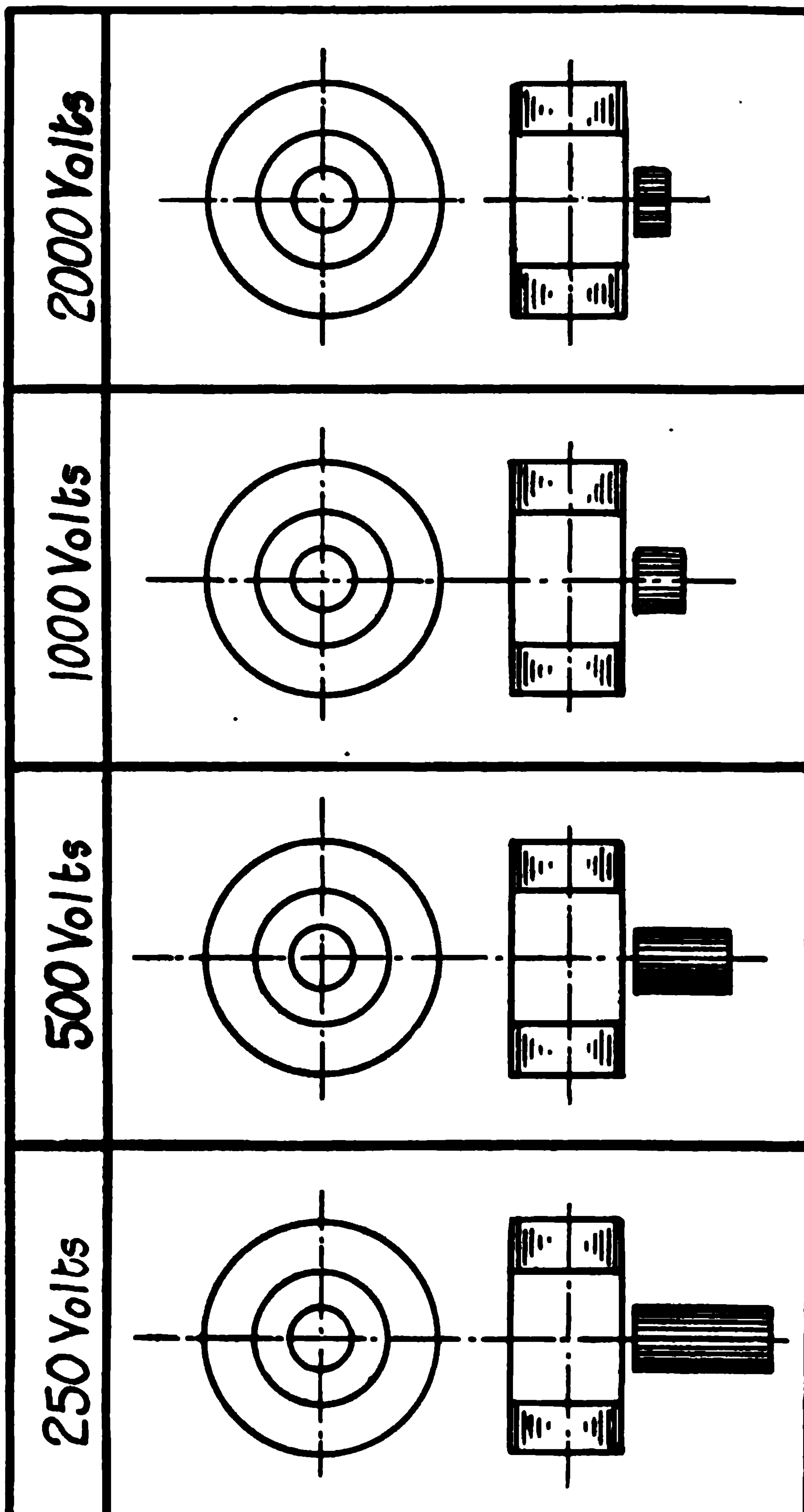


Fig. 296.—Outline sketches of a 1000 kw. 1000 r.p.m. continuous current generators, for different voltages, showing reduced commutator lengths effected by the use of special brushes.

with that of the sets of much larger rated capacity which could be employed with piston engine plants. It will be readily seen that the steam consumption will amount to from 25 per cent to 50 per cent

more for such a turbo continuous current station than for a station equipped with a few piston engine sets, each of large capacity.

Owing to the need for, say, twice as many sets for a given aggregate capacity when steam turbines are employed, the saving in engine room floor space will, contrary to the customary contention, be by no means marked, and the boiler room will have to be much larger than for the steam raising equipment which would suffice for economical piston engine plant. It is hardly necessary to allude to the greatly increased cost for condensing plant which is incurred when turbine sets are installed.

Thus while steam turbines have a wide field of usefulness for driving alternators, their employment for continuous current sets — at any rate, pending the more general introduction of higher continuous current voltages — is merely one more instance of the non-discriminating attitude of the majority of engineers. They must be either for or against each new device. They cannot see that each has its field of usefulness, and that the engineering profession is the richer as the result of the wider variety of systems at its disposal; they must, on the contrary, relegate all but the newest fad to the scrap heap, and introduce this newest fad, not only for the work where it may be used to good advantage, but also for all sorts of purposes for which it is inherently inferior.

In our investigations in the preceding chapters, we have shown that beyond certain limits, it is necessary, in order to obtain good commutation, to depart from the ordinary type of continuous current dynamo to the extent of introducing interpoles or some special auxiliary commutating device. It was thought that the single departure of providing interpoles, rendered the investigations sufficiently complex without introducing the further questions as regards the particular means of neutralising the reactance voltage and other disturbing influences emanating from the armature. Otherwise expressed, we may say that our intention has been that the interpoles introduced in many of our preliminary designs should be taken as more or less diagrammatic indications of auxiliary means in general. As a matter of fact we are decidedly of the opinion that for a large part of the range of ratings where auxiliary means become necessary, interpoles afford the most expedient method of providing such means. It is, however, well known that interpoles proportioned on the lines we have described, are merely sufficient for neutralising the reactance

voltage, and that their effect in neutralising armature distortion is too local to be of importance. The distorting component of armature interference must, in certain designs, also be neutralised.

Opinions differ as to the range of outputs over which it is desirable to endeavour to neutralise the armature distortion. In view of the distinctly objectionable features of distributed compensating windings, of the very considerable additional expense involved in them, and of the additional thermal disadvantages introduced by these windings, we prefer to resort to them only in the case of ratings where the results would otherwise be unsatisfactory.

There is but scant experience with designs with more than some 20 to 30 average volts between adjacent commutator segments. When this is the average voltage, the maximum voltage may, as the result of distortion of the flux distribution in machines without compensating windings, rise, at full load, to some 45 to 60 volts. Such values promote flashing over, and justify the employment of distributed compensating windings so proportioned that on the stator side of the air gap a magnetomotive force is present closely conforming in distribution and intensity to the armature magnetomotive force, but in the opposite direction and consequently neutralising it. In fact, at the points occupied by the interpoles, in interpole designs, means are provided for over-compensation, in order to obtain the flux required for neutralising the reactance voltage in the coils short circuited at the brushes.

It is evident that the compensating winding will at least equal the armature winding as regards weight of copper, since we have the same number of turns carrying the same current, and that the loss in the compensating winding will be of much the same amount as the armature I^2R loss. Thus we have a substantial addition to the outlay for material, and to the internal loss in the dynamo. The additional labour cost incident to the use of distributed compensating windings is by no means inconsiderable. Some firms abandon salient poles and substitute a laminated stator in which the field windings are placed in slots distributed over the inner surface of the laminated core.

The reader will now be prepared for the rather heterogeneous collection of examples from practice to which it is proposed to direct his attention. While highly ingenious designs are everywhere in evidence, anything approaching elegance in appearance is conspicuous by its absence.

Fig. 297 is of interest for the sake of comparison with modern designs. The figure represents the armature of Parsons' original 7.5-kw. turbo-dynamo, which ran at the abnormal speed of 18,000 R.P.M. The armature was only 3 inches in diameter, and was built up of iron plates placed direct on the shaft. The winding was on the surface,

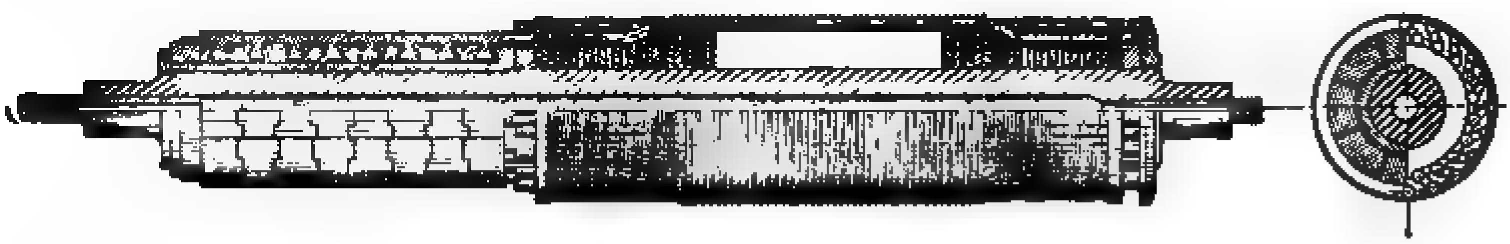


FIG. 297. — Armature of Parsons' first turbo-dynamo.

and was retained by a layer of steel binding wire running the whole length of the armature. The commutator was of curious construction, consisting of a number of cast bronze segments alternating with steel dove-tail rings which took up the radial stresses over the entire

FIG. 298. — Armature for Oerlikon 200 kw. 220–250 volt 3000 r.p.m. continuous current generator.

length. The peripheral speed of the commutator was 57 metres per second. This construction was adopted by Parsons for small sizes.

A small diameter usually permits of mounting the armature stampings directly on the shaft. This is sometimes advantageous from the mechanical standpoint, as the spider is dispensed with. On the other hand, it does not lend itself to effective ventilation.

Fig. 298 shows a typical finished armature by the Oerlikon Company for a 200-kw. machine at 3000 R.P.M. and 220–250 volts. It will be noted that the armature slots are not wide open, but have overhanging tips to retain the conductors against centrifugal force. The commutator segments are held together by very massive steel rings. The field frame for this machine is shown in Fig. 299.

FIG. 299. — Field system for Oerlikon 200 kw. 220–250 volt 3000 r.p.m. continuous current generator.

From the mechanical standpoint, it would be preferable to employ half closed armature slots, or even tunnels, but this is not in accordance with the electrical requirements, as the inductance of the coils would be increased, and also former winding would not be possible. Some makers use a half closed slot, as in Fig. 298, with a longitudinal holding strip in the mouth of the slots. A totally open slot

with a dove-tail wedge at the mouth should be sufficient for retaining the conductors. In some cases the armature is bound over with steel wire, as in the early Parsons machines.

Either of these methods is sufficient for the conductors in the slots, but the end connections require special provision to be made. The two methods in common use are firstly steel binding wires, and secondly the use of cylindrical metal covers completely enclosing the end windings. The method adopted for keeping the end connections in place is not shown on the armature in Fig. 298.

FIG. 300. — Method of holding down end connections by means of metal end-bells.

The first mentioned method is illustrated in Fig. 294 for the 1000-kw. machine. The windings bed on a cylindrical drum, and are bound down with several layers of wire, heaped up where the stress is greatest.

Fig. 300 shows windings held by a cylindrical end-bell. The material for the end-bells must be very homogeneous, and should preferably be rolled or forged rather than cast. It is here that steel wire has some advantage. The material generally used for end-bells is bronze, manganese bronze, or nickel steel.

The peripheral speed should not exceed 100 metres per second (20,000 feet per minute) as an outside limit, and it is preferable to keep well within this speed. In fact, we rarely find that the peripheral speeds of the armatures of continuous current machines already in operation exceed 80 metres per second. The average speed at present employed is from 50 to 60 metres per second.

FIG. 301. — Method of reducing centrifugal force at the surface of end connections.

Fig. 301 shows a method adopted by the Austrian Union Company

for reducing the diameter over the end connections by bending them down radially, and thus reducing the diameter and stress in the end-bell.

Commutators. — Owing to the high peripheral speeds encountered, the usual form of commutator construction, employed in machines where the peripheral speed of the commutator has rarely exceeded some 16 metres per second, is no longer adequate.

The peripheral speed of the commutators of turbo-dynamos sometimes exceeds 40 metres per second. The commutator is necessarily long, and the high centrifugal forces on the segments would lead to their bowing outwards, and to high bars, if the ordinary construction with dove-tail clamping rings at each end of the long segment were used.

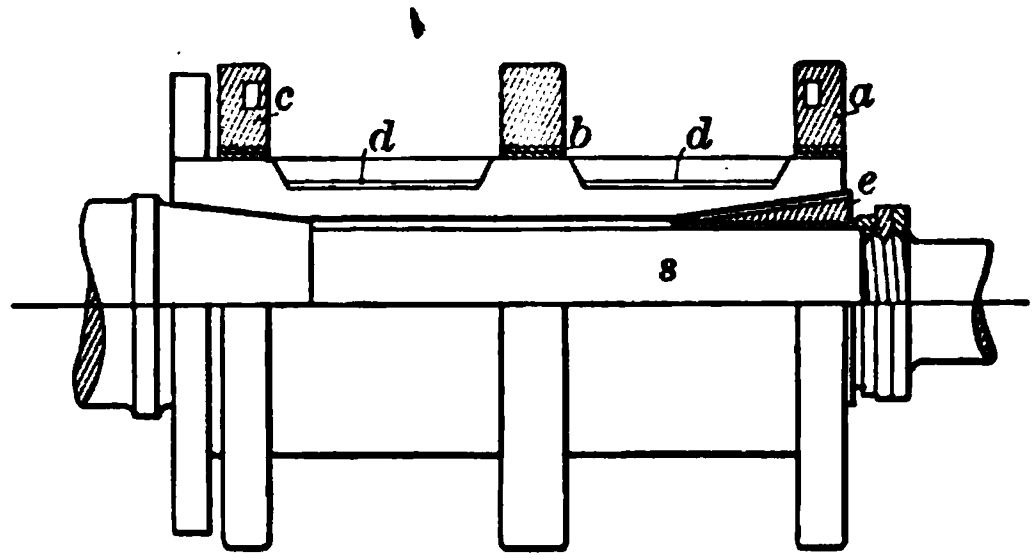


FIG. 302. — Sketch showing typical high speed commutator construction.

In some cases the commutator has been subdivided longitudinally into several shorter ones by cutting the segment into several short lengths with a dove-tail clamping ring between neighbouring parts of the segments, something after the style of the early Parsons commutator already shown in Fig. 297. Such a construction requires a great deal of machining, and is consequently expensive, and none too satisfactory.

A principle of construction which is now very widely employed for high speed commutators is shown in section in Fig. 302. The centrifugal forces are taken up by steel rings *a* shrunk over the commutator and insulated therefrom by insulating rings *b*. The grooves *c* turned in the end-rings afford a place for the application of balance weights. Having in view the wear that will take place on the commutator, the segments should be strong enough, after wearing down, not to bulge between the shrinking rings. To this end it is well to adhere to the rule of proportioning the segments so that

when worn to about one half their original depth they are strong enough to resist the tendency to bend and are still of sufficient cross section to carry the current without overheating. A number of grooves, *d*, in the segments, serve to indicate when the commutator is worn down to its limit and needs renewing.

In the construction of these commutators the segments are assembled, clamped in position, and the holding rings shrunk on over

Fig. 303. Fig. 304.

Figs. 303–304. — Methods used for interlocking commutator segments.

their layers of mica. The commutator is now a self-contained structure and is readily machined, inside and out. When slipped on the shaft it is held up on to a taper on the shaft, *s*, by the taper collar, *e*. A commutator constructed on these lines has already been shown in Fig. 298.

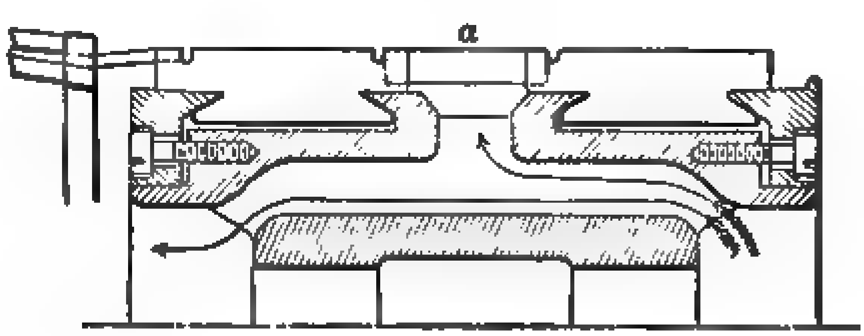


Fig. 305. — Method for obtaining improved commutator ventilation where sufficient diameter is obtainable.

The Brush Company use an additional safeguard in their commutators. This consists in locking each segment into its neighbour by means of a longitudinal groove in the next segment. The segments are drawn to the section indicated in Fig. 303, which diagrammatically illustrates the idea. Fig. 304 indicates a somewhat similar method which has been employed, and which was invented by James F. Burke.

In Fig. 305, a means of ventilating a high speed commutator is shown. This is due to Messrs. Siemens Brothers. This construction requires a reasonably large internal diameter. The commutator is built up of two sets of short segments, which are connected at *a* by copper strips, these strips being of less width than the commutator segment. Thus air spaces are provided between each connection, and air circulation is obtained as indicated by the arrows in Fig. 305.

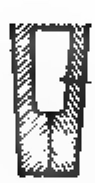


FIG. 307.

FIGS. 306–307. — Ventilated commutator patented by Siemens Bros., Dynamo Works Limited.

In machines where the internal diameter is limited, this scheme is impracticable, as there is no room for a ventilating channel in the commutator spider. Messrs. Siemens Brothers Ltd., in conjunction with C. M. Toplis, have patented a scheme of commutator ventilation which provides an effective means of cooling commutators where the diameter is limited. This method is illustrated in Figs. 306 and 307. Each commutator bar is double and the halves are formed with a longitudinal passage between them throughout their length, and

through these passages air is drawn by blades attached to the commutator at the middle opening. The method of channelling the segments is shown in Fig. 307 where a compound segment is shown in section.

2 4 2 4

FIGS. 307 A and B. — Miles Walker's Commutator Construction.

Figs. 307 A and B illustrate a new construction of commutator patented by Miles Walker and employed by the British Westinghouse Co. This type of commutator embodies the advantages of shrink rings for retaining the segments, and also avoids vibration of the

brushes due to radial vibration of the commutator. The working faces of the commutator are in several vertical planes formed by providing large U-shaped recesses in the commutator. In this way a large part of the loss at the brushes is done away with and more satisfactory commutation is obtained, as vibrations due to the eccentricity of the commutator do not affect the working of the brushes. Two types of construction are shown in Figs. 307 A and B, the former with the shrink rings at the extreme outer periphery of the commutator, and the latter with shrinking bands at the bottom of the recesses.

Brown Boveri & Co.'s Continuous Current Turbo-Dynamos.

The following general description is typical for the majority of Messrs. Brown Boveri's continuous current turbo-dynamos.

Armature Cores. — The core plates are composed of special quality annealed iron of high permeability and low hysteresis loss, and are insulated one from the other by thin paper sheets.

Armature Windings. — The conductors are arranged in slots, and are held in place by hard wood wedges. The end connections are held in place by metal caps made from a special alloy of great tensile strength.

Commutator. — The commutators are of the shrink ring construction, the rings being shrunk over solid mica bands placed at intervals along the commutator. In the small designs a flexible connection is provided between the commutator segments and the armature conductors by means of short lengths of cable.

Field System. — The field windings are distributed in slots in a manner similar to that employed in the stator of an induction motor. The windings consist of two distinct parts, namely, the usual shunt winding and a compensating winding in series with the main circuit.

The series winding is arranged at one half of the polar angle in advance of the shunt winding. A reversing field is produced at the point of commutation, the strength of which is proportional to the load. This series compensating winding, being equally distributed around the exterior of the armature, also prevents distortion of the main field by the armature field; thus the machine maintains a practically constant voltage for all loads.

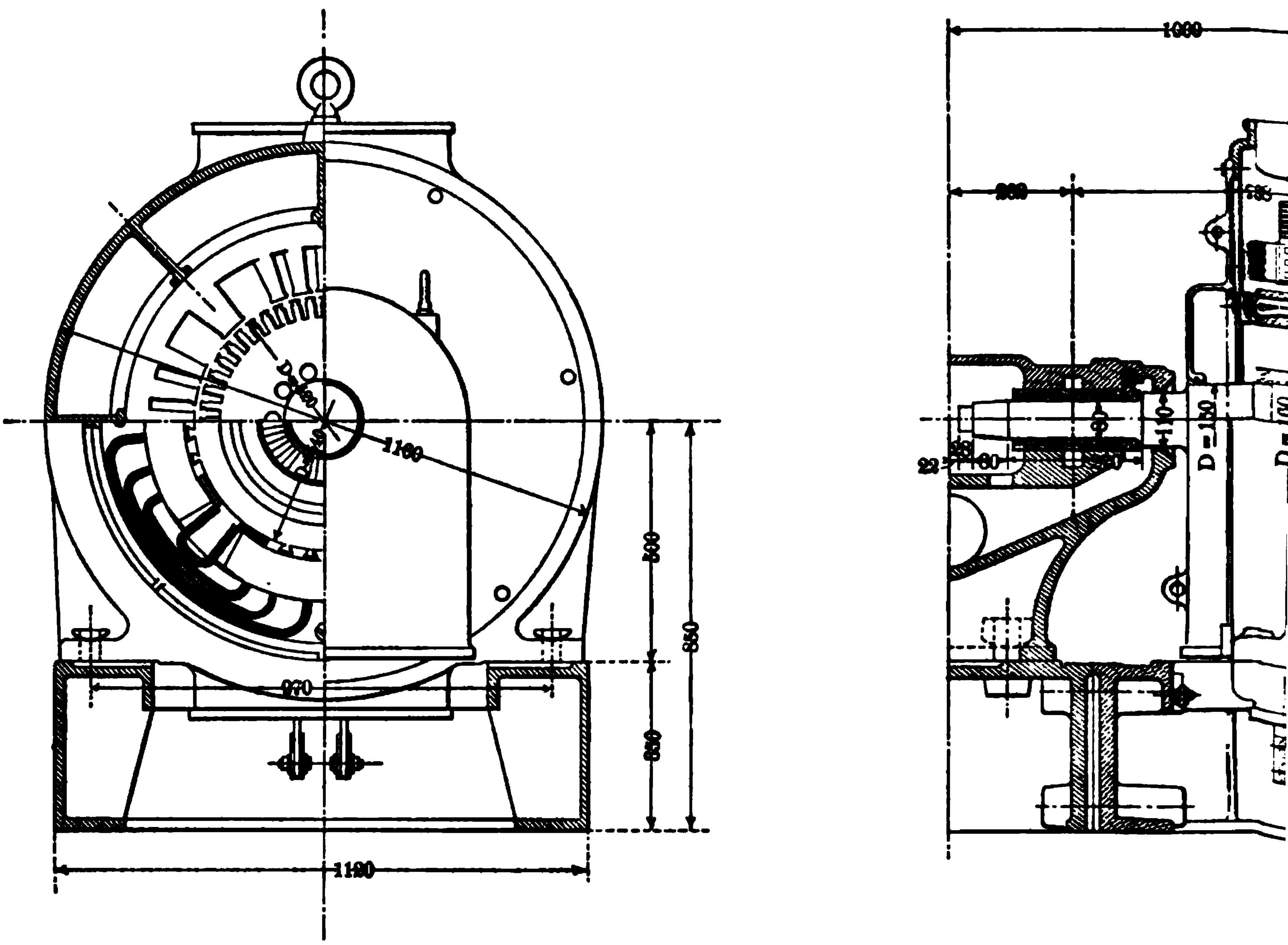


Fig. 308. — Brown Boveri 250 kw., 2[illegible]

00 R.P.M., 150-volt Turbo-Generator.

Excitation. — In a type of machine employing distributed windings, where it is required to run two or more dynamos in parallel, it becomes necessary to separately excite the shunt field, as there is a possibility of a machine having the direction of its field reversed causing it to run as a series motor and at a dangerous speed. These machines all have a separate exciter mounted on the main generator shaft outside the main bearing.

Ventilation. — The Brown Boveri patented system of induced ventilation is employed in the majority of their machines. The two ends of the machine are completely enclosed by metal covers. The cool air enters the machine from under the bed plate at the coupling, and is drawn along ducts parallel to the shaft, whence it passes through the ventilating ducts in the armature core, next passing through the ducts in the field core and round the windings, and finally leaving the machine by the chimney-shaped opening in the top of the case.

Brushes. — Special compound carbon and metallic brushes are used. These are referred to in further detail in Chapter XIX. In the larger sizes, carbon brushes are fixed in advance of the main brushes, the object being to prevent flashing with heavy overloads.

A study of some drawings and photographs of these machines will be of assistance in explaining the general principles. In Fig. 308, details of a Brown Boveri continuous current high speed dynamo of 250-kw 2700 R.P.M. 150 volts are given. Commencing at the right hand side of the illustration the first detail encountered is the separate exciter, the armature of which is mounted directly upon the main shaft. A small extension carries the commutator spider. This construction is sufficiently rigid to carry the rotating part of the exciter without the addition of an outside bearing. The exciter brush gear is carried on a substantial bracket bolted to the exciter yoke, which in turn is fixed to the main pedestal. Passing from the exciter to the main commutator the most striking feature is the substantial size of the brush gear and the shrink rings.

No internal ventilation of the commutator is provided, the commutator losses being small on account of metallic brushes being used. The method of retaining the end connections by means of metal covers is clearly shown in the section through the armature core. In the end elevation, on the left hand side of the figure, the arrangement of the distributed field winding is indicated.

FIG. 309. — Field frame and core, without windings, of a 135 kw. Brown Boveri continuous current 3000 R.P.M. turbo-generator.

FIG. 310. — Field frame, core and windings, of a 135 kw. Brown Boveri continuous current 3000 r.p.m. turbo generator.

The principal dimensions of this 250 kw., 2700 R.P.M., 150 volt machine are as follows:

Dimensions in Centimeters.

Armature diameter (D)	52
Gross length of core (l_g)	37
Total length over armature winding (L)	70
Number of ventilating ducts	4
Number of armature slots	48
External diameter of frame	116
Overall length of machine	308
Commutator diameter	26
Overall length of commutator segment	78

TECHNICAL DATA

Output coefficient		0.93
Armature peripheral speed	meters per second	73
Commutator peripheral speed	meters per second	36.5

FIG. 311. — Finished armature and commutator of a Brown Boveri 135 kw. continuous current generator at 3000 R.P.M.

In Fig. 309 the four pole field frame and core, without windings, of a Brown Boveri 135-kw. 3000 R.P.M. continuous current turbo-dynamo is shown. The large slots will contain the main field winding (shunt), and the smaller slots the series compensating winding.

In Fig. 310 a similar core is shown, but with the windings in place.

This is a 2-pole machine, the main field coils appearing on the right and left hand sides. The method of arranging and supporting the end connections should be especially noted.

An illustration of the armature of the 135-kw. machine is shown in Fig. 311. In this instance binding wire is employed for holding the end connections in place, instead of metal covers such as are used in the larger size machines.

Particulars of a Richardson Westgarth & Brown Boveri 4-pole 1000-kw. 550-volt 1250 R.P.M. Continuous Current Turbo-Generator.

General. — This machine is direct coupled to a Parsons single flow turbine built by Messrs. Richardson and Westgarth.

The dynamo is of Messrs. Brown Boveri's standard type, with a laminated stator having reversing teeth and Deri windings. The machine is totally enclosed, with an outlet for air at the top. The ventilating scheme consists in drawing in air at the shaft from holes in the bottom of the end shields, which are provided with baffles to direct the air into the centre of the rotating armature. The air passes up the armature ducts and through the ducts in the stator core which are placed opposite those on the armature and are of the same number. Thence it circulates through the stator casing, leaving by an outlet about 60 cms. by 30 cms. at the top. There is a considerable draught out from this opening.

The overall diameter of the machine is about 6 feet. The generator is held on the turbine bed plate by four 2-inch bolts. The main brushes consist of thin sheet copper with a Morganite graphite pilot brush in front of every alternate copper brush.

The machine was subjected to a 3 hours' test at half voltage but with 2100 amperes, which is 1.17 times the full load current of 1800 amperes. It is stated that the brushes required no shift, and that there was no visible sparking. The commutator was in very good condition after running. It is stated that the machine will stand 25 per cent overload without brush shift. The machine was quite cool at the commutator and stator, but the actual temperature rise was not given. It is stated that the rise does not exceed 40 degrees C.

Speed Regulation. — The rise in speed when full load is suddenly thrown off, is stated to be

7 per cent momentarily and 2½ per cent permanently.

FIG. 312. — Armature of Richardson Westgarth & Brown Boveri's 1000 kw. 550 volt 1250 R.P.M. 4-pole continuous current turbo-generator.

FIG. 313. — Richardson Westgarth & Brown Boveri 1000 kw. 1250 R.P.M. 4-pole continuous current turbo-generator.

Excitation. — The machine is separately excited with a small exciter mounted on an extension of the dynamo shaft beyond the bearing. The rating of the exciter is:

6 h.p. 6-pole, 100 volts, 38 amperes, 1250 R.P.M.

Fig. 314. — Large steam turbine unit installed at the Rhenish Westphalian Works at Essen, consisting of a 10,000 h.p turbine coupled to a 5000 kw. alternator and a 1500 kw. continuous current generator.

The armature of the main dynamo has a diameter of about 1 metre and a gross core length of about 50 centimetres. This gives a peripheral speed of about 70 metres per second and an output coefficient of 1.54. The shaft diameter in the bearings is about 25 centimetres.

The end windings are retained by bronze cylindrical caps about 2½ centimetres thick, which also carry radial vanes which circulate the ventilating air. At the commutator end, the cylindrical cap is screwed on to an internal spider with screws spaced at intervals of about 15 centimetres. The leads to the commutator are bent round the screws at places where they occur.

The commutator is about 70 centimetres diameter by 90 centimetres overall length, giving a peripheral speed of about 30 metres per second.

FIG. 315. — Armature of a 1500 kw. continuous current generator belonging to the set shown in Fig. 314.

Brush Gear. — The brush spindles are supported at the bearing end from a rocker carried on the bearing, and at the machine end they are screwed into the machine end cover. There are 4 spindles, each carrying 12 brush holders. The brushes are of galvano metallic foil set on the commutator at an angle of about 30 degrees. Every alternate brush has a pilot brush of carbon.

Stator. — The stator core has the same axial length and number of ventilating ducts as the armature. The field coils are form wound and are contained in large open slots. The stator also carries a series winding and a distributed compensating winding. These

two windings are laid out in two ranges one behind the other. The conductors consist of thick copper strip let into semi-closed slots and secured by wooden wedges. The end connections are bare and are put on after the bars are in place.

The reversing pole consists of a large tooth extending the whole length of the armature. The end portions of all the stator windings are bound on to strip iron bridge pieces screwed to the frame.

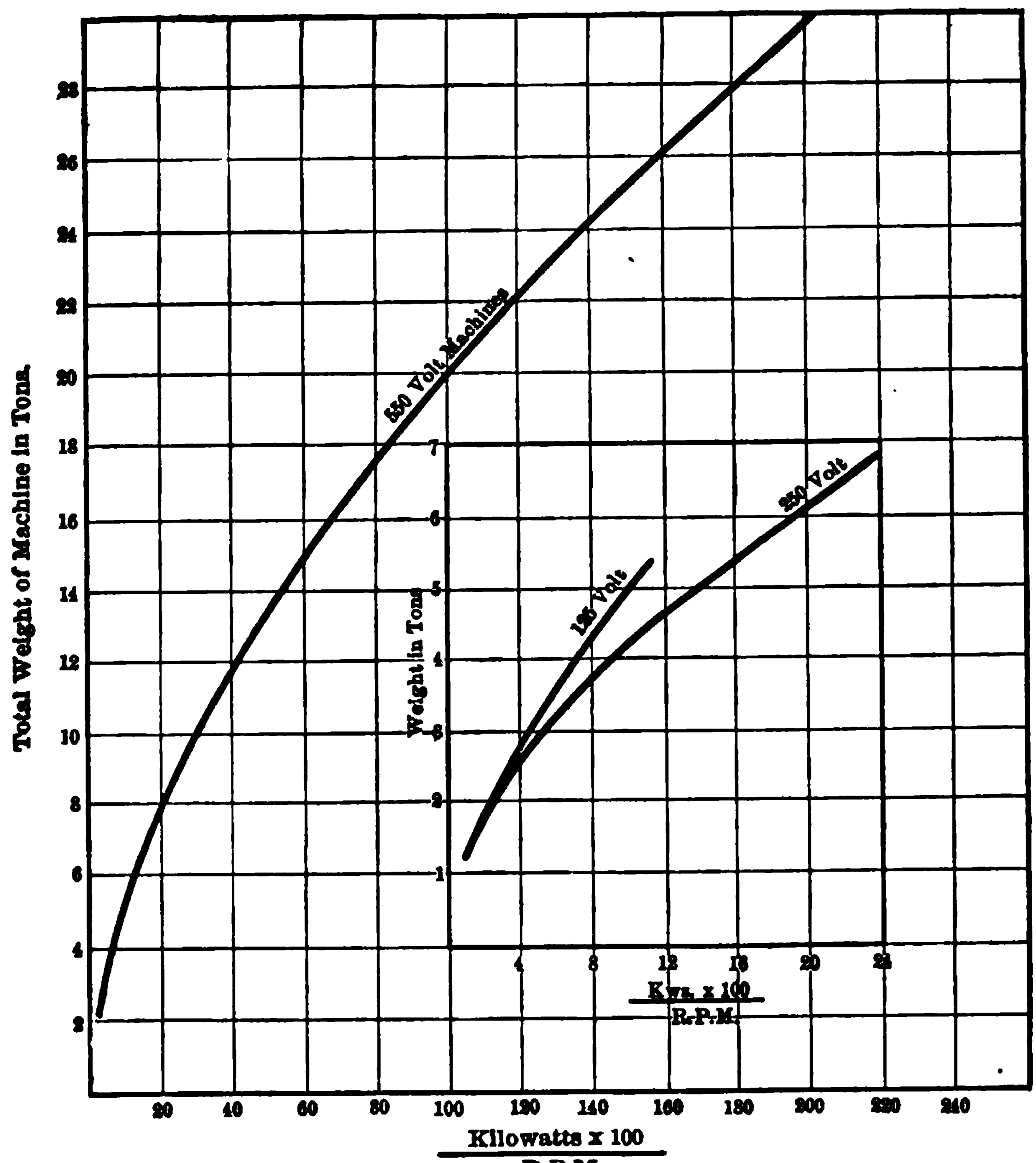


FIG. 316. — Curves plotted from Table 70 showing total weight of continuous current turbo generators in terms of $\frac{\text{kilowatts} \times 100}{\text{Revs. per min.}}$.

The completed armature and commutator of the 1000-kw., 1250 R.P.M. turbo-dynamo is illustrated in Fig. 312. The complete generating unit, turbine and dynamo combined, is shown in Fig. 313. This illustration clearly shows the substantial construction of the brush rocker and gear.

In Fig. 314 a steam turbine generating set installed at the Rhenish Westphalian Electricity Works at Essen is shown. The steam turbine is of 10,000 h.p. capacity direct coupled to a 5000-kw. alternator and a 1500-kw. 600-volt continuous current generator; the speed of the set being 1000 R.P.M. The outside dimensions of the set are as follows: Total length 18.5 metres; breadth 3.0 metres; height 2.6 metres;

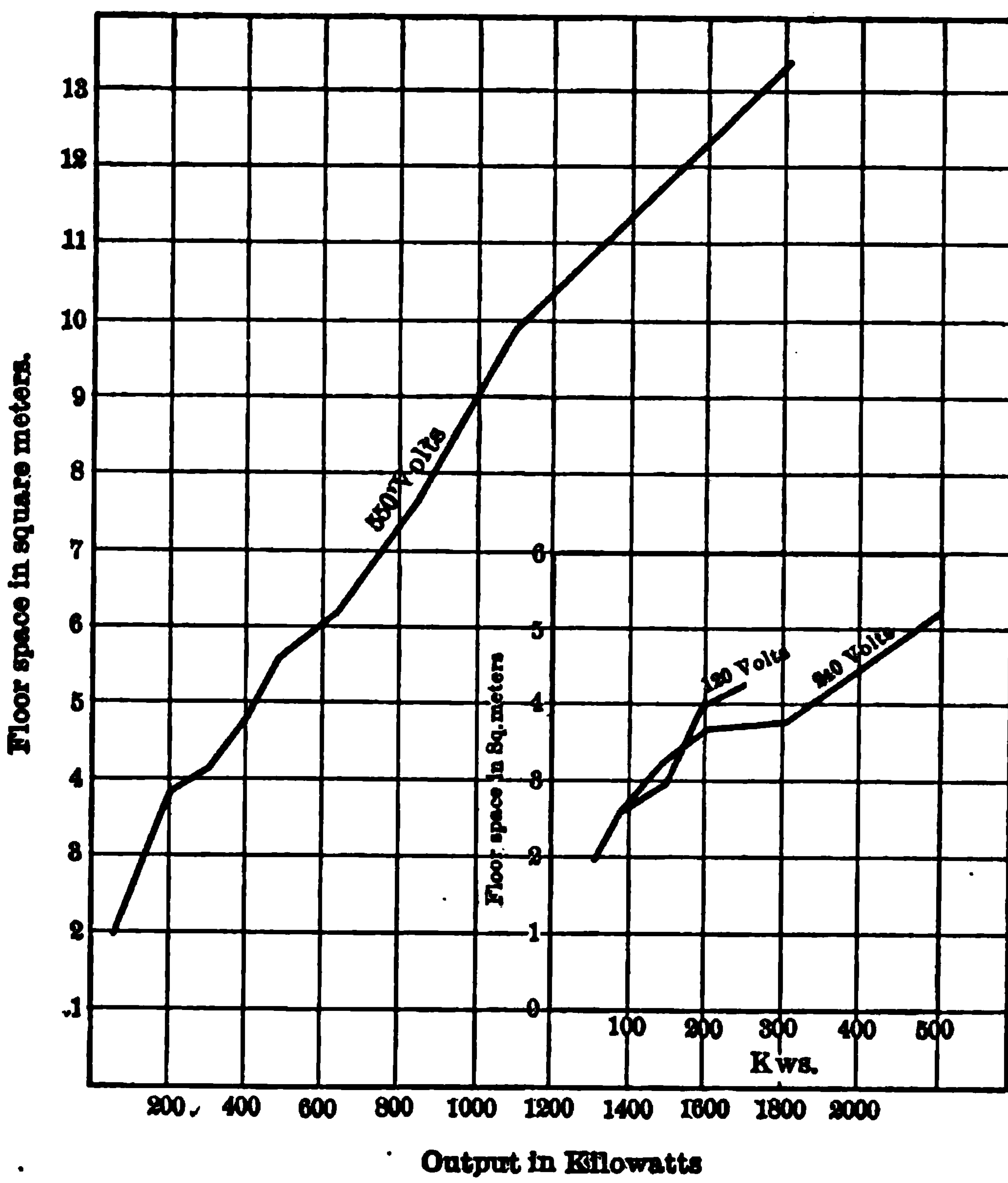


Fig. 317.—Curves plotted from Table 71 showing floor space required by continuous current turbo generators of various outputs.

total weight 190 tons. The armature of the 1500-kw. continuous current generator belonging to this set is shown in Fig. 315.

Messrs. Brown-Boveri have standardised their continuous current turbine generators up to a rated output of 1250 kw. In Table 70, data are given showing the relation between total weight and the kilowatts output and speed for the standardised continuous current turbo-generators.

In Table 71, for the same group of machines, the corresponding overall dimensions and floor space in square metres are given.

The curves in Figs. 316 and 317 are plotted from the data in Tables 70 and 71.

TABLE 70.

APPROXIMATE TOTAL WEIGHT IN METRIC TONS FOR BROWN BOVERI & CO.'S STANDARDISED LINE OF CONTINUOUS CURRENT TURBO-GENERATORS.

120 Volts.				240 Volts.				550 Volts.			
Output in Kw.	Speed in R.P.M.	Kilo-watts per 100 R.P.M.	Approx. Weight in Tons.	Output in Kw.	Speed in R.P.M.	Kilo-watts per 100 R.P.M.	Approx. Weight in Tons.	Output in Kw.	Speed in R.P.M.	Kilo-watts per 100 R.P.M.	Approx. Weight in Tons.
65	3500	1.85	1.6	65	3500	1.85	1.55	65	3500	1.85	1.5
90	3000	3.0	2.1	90	3200	2.8	2.1	90	3500	2.6	2.0
150	3000	5.0	3.1	150	3000	5.0	3.15	150	3200	4.7	3.0
200	2700	7.4	4.1	200	2800	7.15	3.9	200	3000	6.6	3.75
250	2400	10.5	5.1	300	2600	11.5	4.4	300	2800	10.7	5.3
				400	2300	17.5	5.5	400	2500	16.0	6.9
				500	2100	23.8	7.2	500	2100	23.8	8.5
								650	1800	36.0	11.0
								850	1500	56.5	14.5
								1100	1250	88.0	18.5
								1500	1000	150.0	25.0
								1800	900	200	30.0

TABLE 71.

SHOWING THE FLOOR SPACE IN SQUARE METRES REQUIRED FOR MESSRS. BROWN BOVERI & CO.'S STANDARDISED LINE OF CONTINUOUS CURRENT TURBO-GENERATORS.

120 Volts.					240 Volts.					550 Volts.				
Output in Kw.	Speed in R.P.M.	Floor Space.			Output in Kw.	Speed in R.P.M.	Floor Space.			Output in Kw.	Speed in R.P.M.	Floor Space.		
		Length in Metres.	Breadth in Metres.	Area in Sq. M.			Length in Metres.	Breadth in Metres.	Area in Sq. M.			Length in Metres.	Breadth in Metres.	Area in Sq. M.
65	3500	2.6	.75	1.95	65	3500	2.6	.75	1.95	65	3500	2.46	.75	1.85
90	3000	2.37	.85	2.5	90	3200	2.97	.85	2.5	90	3500	2.82	.85	2.4
150	3000	3.27	.90	2.95	150	3000	3.27	.95	3.10	150	3200	3.12	1.00	3.12
200	2700	3.58	1.10	3.95	200	2800	3.58	1.05	3.80	200	3000	3.25	1.16	3.8
250	2400	3.78	1.10	4.15	300	2600	3.64	1.05	3.80	300	2800	3.58	1.16	4.2
					400	2300	3.78	1.20	4.55	400	2500	3.81	1.3	4.9
					500	2100	4.06	1.30	5.30	500	2100	4.2	1.45	6.1
										650	1800	4.12	1.5	6.2
										850	1500	4.80	1.6	7.7
										1100	1250	4.98	1.85	9.2
										1500	1000	5.29	2.25	12.0
										1800	900	5.64	2.4	13.5

500-Kw. 1500 R.P.M. 250-Volt Continuous Current Turbo Generator of Siemens Bros. Dynamo Works.

Fig. 318 shows a generator driven by a Willans-Parsons high pressure steam turbine. The generator is rated at 500 kw. on 230 to 250 volts and 1500 R.P.M. It has a cast steel yoke and four laminated main poles. Placed between these are four commutating poles, which are also laminated, and are provided with a pair of laminated

FIG. 318. — 500 kw. 1500 r.p.m. 250 volt continuous current turbo-generator built by Siemens Bros. Dynamo Works.

yokes which are placed on each side of the main yoke thus forming a magnetic system which is completely isolated from the main magnetic system at all points.* The armature is of the slotted drum type, the slots being provided with grooves, which receive suitable wedges for holding the winding firmly in place, no binding wire being used on the core. The end connections of the winding are held in place by steel bands and manganese bronze covers. The commutator

* This arrangement with independent yokes is an admittedly undesirable construction which has been abandoned.

is of interest owing to the special construction necessitated by the large current of about 2000 amperes which the machine has to supply, and to the fact that carbon brushes are used throughout. The commutator has five cells, the middle cell being used solely for purposes of ventilation and cooling. It is stated that the brushes require no alteration in position with changes of load, and that the machine runs sparklessly up to heavy overloads. The wear on the brushes is stated to be inappreciable.

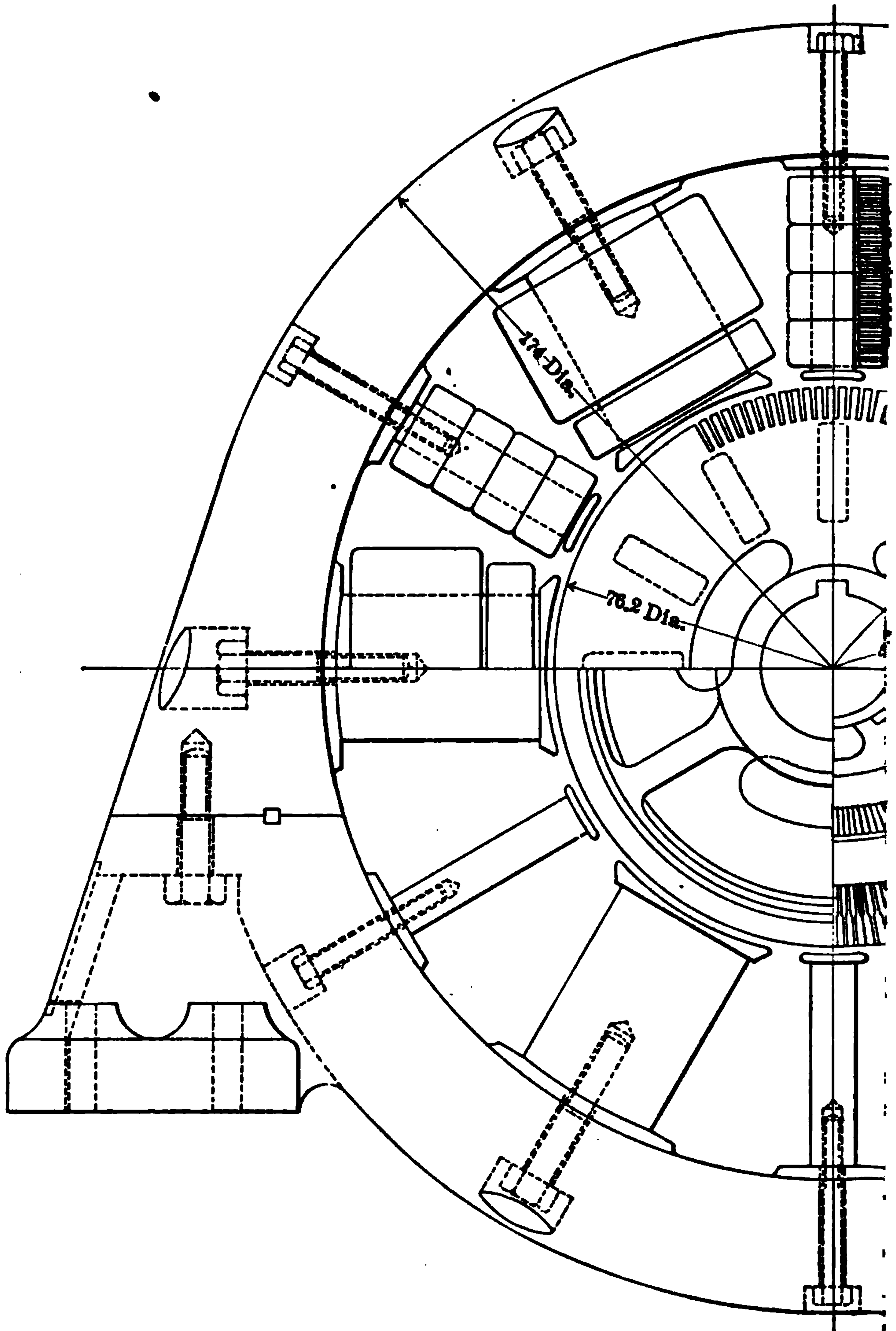
An earlier design by this firm has been described in *The Electrician* for April 6, 1906, and is accompanied by outline drawings reproduced in Fig. 319. The machine is for a rating of 750 kw. and 500 volts at a speed of 1600 R.P.M. Two of these machines were installed at the Dickinson Street station of the Manchester Electricity Works. They were supplied with a series winding in addition to the shunt winding, so that they might be used on the lighting or traction circuits. As shunt machines they were intended to supply 1870 to 1670 amperes at 400 to 450 volts, and as compound machines, up to 1360 amperes at 500 to 550 volts.

As will be seen from Fig. 319, which shows the machine in end elevation and part section, the armature stampings were mounted directly on the shaft, without any separate core centre. The core was divided into six equal sections by means of five ventilating ducts each 6.5 mm. wide, to which the air was supplied through axial channels formed by holes stamped in the plates, the radial vents being made by spacing the plates by brass distance pieces. The winding was retained in the slots by means of fibre wedges, fitted into notches at the top of the teeth, and no binding wire was employed. Bronze core ends supported the bent portions of the coils, which were retained in position by substantial coned shields of manganese bronze, carefully machined all over.

The copper bars were held in against centrifugal force by steel rings shrunk on. The rings were insulated from the copper by thick rings of mica. Carbon brushes were used, and the commutation has been stated to have been good. There was, however, a tendency to flash over at the brushes, which Messrs. Siemens were at the time sanguine of overcoming by the use of a new design of brush gear and by the addition of insulating shields.

The magnet yoke was of cast iron, split approximately horizontally to allow of the armature being readily taken out. A slight departure

FIG. 319. — 750 kw. Siemens continuous current generator for turbine drive with commutator poles. Speed 1600 R.P.M.


174 Dia.
76.2 Dia.

from the horizontal was necessitated by the position of the auxiliary poles, as indicated in the illustration. The main poles, which were laminated, were bolted to the yoke, and the pole shoes consisted of iron castings embracing the stampings and secured to them by rivets.

The auxiliary poles belonged to an entirely distinct magnetic system as shown in the drawing, comprising a separate yoke and four poles. This yoke was made up in two rings, rather less in diameter than the main yoke: and the poles, which had the same length, measured along the axis of the machine, as the main poles, and laminated, projected into the space between the main poles. They were excited by a winding in series with the armature, so designed as to give a reversing field of correct strength for commutation. The commutation magnetic system was not saturated, and therefore the flux was practically proportional to the load on the machine, and no change in the position of the brushes was expected to be required for varying loads.

The field coils, both on the main and auxiliary poles, were former wound and were heavily insulated with tape. A diverter rheostat was provided, by means of which a portion of the main current flowing round the commutating poles could be shunted, so that a certain amount of adjustment could be made of the relative strength of the commutating flux to ensure the best results.

Dimensions and Data of Siemens 750-Kw. Turbo-Generators.

Dimensions in Centimetres.

Number of poles	4
Speed in R.P.M.	1600
Voltage	500–550
Armature diameter (D)	90
Armature gross length (λg)	46
Armature overall length	82
Number of ventilating ducts	6
Width of each duct	0.63
Yoke external diameter	165
Yoke internal diameter	200
Yoke width parallel to shaft	58
Magnet core breadth parallel to shaft	38
Magnet core width at right angles to shaft	26
COMMUTATOR	
Diameter	50
Useful length	80
Total length	115

TECHNICAL DATA

Cross section of magnet core — sq. cms.		1000
Assumed flux density in magnet core		16,000
Total flux generated, in megalines		1.60
Assumed leakage factor		1.35
Armature flux per pole		11.8
Probable number of face conductors		176
Probable reactance voltage (at 550 volts)		19
Average volts per segment (at 550 volts)		25
Output coefficient		1.25
Armature peripheral speed	meters per second	75
Commutator peripheral speed	meters per second	41.5

A study of a continuous current turbo-generator of a similar output and speed to the Siemens machine just described but of very different

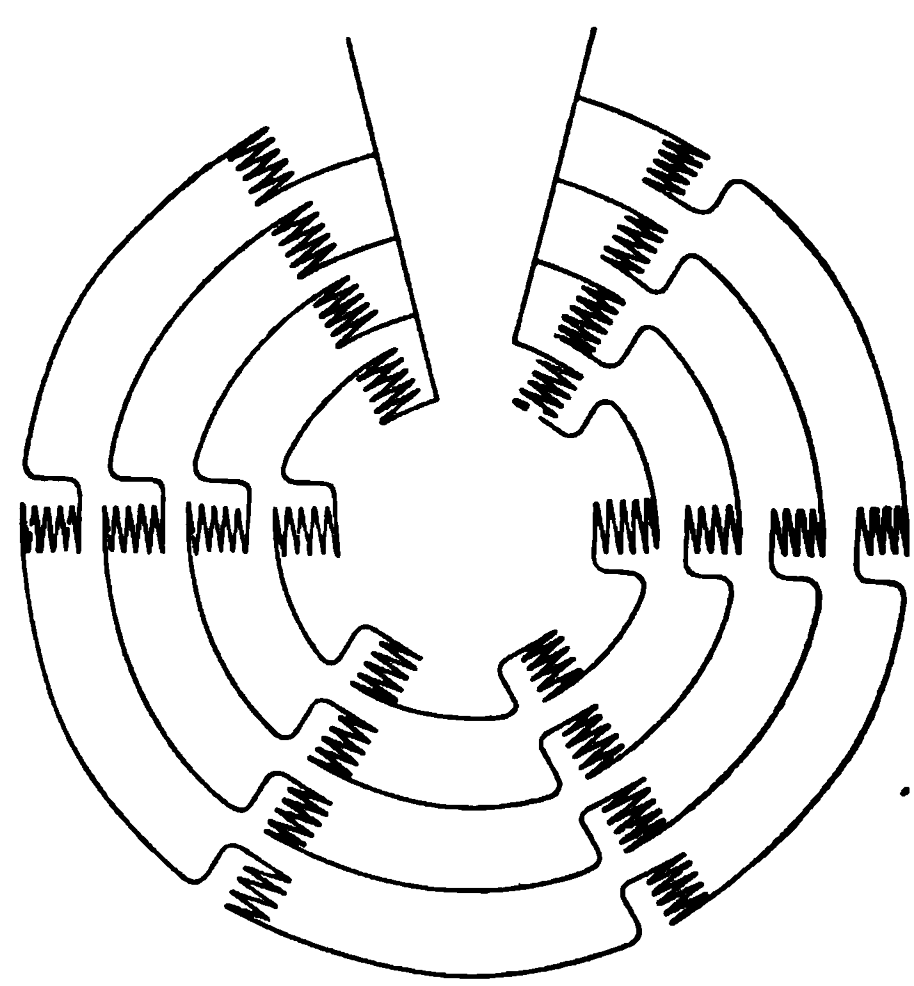


FIG. 321. — Diagram of winding for auxiliary field circuit connections of 750 kw. high speed continuous current generator.

design, was published by one of the authors in the *Electrical Review* of New York for January 20, 1906.

In the following specification, and in the diagrammatic sketches in Figs. 320 and 321, are given the rough outlines for the electromagnetic design for this 750-kw. 250-volt continuous current generator which was designed for a speed of 1500 R.P.M.

Specification of 750 Kw. 250 Volt 1500 R.P.M. Continuous Current Generator.

Armature. — The core plates are stamped in one piece from plates of a thickness of 0.5 millimetre, and are assembled directly upon the shaft, as the required shaft diameter does not permit of an intermediate armature spider. The slot conductors are kept in place by wedges, which in turn are retained by recesses in the sides of the slot, and by binding bands. The end connections are carried on a specially-shaped end-plate, which is curved on its surface, and permits of heaping towards the centre the binding wire holding the end connections.

Commutator. — The segments are built up with the intervening layers of mica and clamped together. Circumferential mica bands are then placed at the middle and at the two ends, and three steel rings are shrunk on over the mica. The interior contour is then machined, and the commutator is secured in place on its sleeve by cones forced in by end-rings. The external surface is then turned. Ventilation of the inside of the commutator is provided for by three channels inside the spider, through which air can be driven by cup-shaped fans at the outer end of each channel.

Brushes. — The brushes are of carbon, or preferably of graphite, of low contact and radial resistance, and high transverse resistance. They are carried in holders of such type as to minimize vibration at the high peripheral speed employed.

Magnet Frame.—The yoke is made from cast iron. This is employed chiefly on account of the greater rigidity and stability thereby obtained, as compared with a cast steel yoke of equivalent magnetic capacity.

Auxiliary Commutating Poles. — The main current of this machine at rated load is 3000 amperes. Taking 1000 amperes through a diverting shunt leaves 2000 amperes, which, if carried through the coils of the six auxiliary poles in a single series, would require about five turns per pole, and each turn would be of inconveniently large cross section. It is also objectionable to use many turns in parallel, owing to the difficulty of obtaining absolutely satisfactory contact at the connections. An alternative would be to put the spools in parallel, but unless very carefully adjusted, the different windings would be of unequal resistance, resulting in varying strengths of current in different spools.

To overcome these difficulties the following arrangement of winding has been adopted: each spool is subdivided into four sections, each wound with $5\frac{1}{2}$ turns of copper strip. The details of the winding scheme are shown diagrammatically in Fig. 321.

The winding is arranged in four parallel circuits with 500 amperes per circuit. Each circuit contains one section of winding on each pole, or 33 turns in series. By this arrangement, the convenience of parallel winding is obtained, without incurring the liability of having varying strengths of field on the different commutating poles, since any inequality in the current in one section is shared by all the poles.

As will be seen from an examination of the design, a high periodicity (in this case 75 cycles per second) is unavoidable. As a fairly high core density is also necessary, in order, in spite of the restricted diameter, to provide access for sufficient air to the interior of the core, thence to flow radially outward through the ventilating ducts, a rather high core loss per kilogram of armature laminations must necessarily be permitted. Liberal provision of radial ventilating ducts must therefore be made, as the total rate of generation of heat in the armature per square decimetre of peripheral radiating surface will be much higher than is otherwise permissible.

The main problem, however, relates to the design of the commutator. Notwithstanding recent very encouraging progress in the development of improved carbon and graphite brushes, and in improved brush holders, a peripheral speed of 35 metres per second is as high as it is yet desirable to go. In order to get sufficient radiating surface to prevent excessive temperature rise, the commutator, as will be seen from the example, is of great length, and correspondingly awkward as regards mechanical design, the more especially so with respect to providing internal ducts for the circulation of air. As a compromise between the mechanical and electrical difficulties, a much higher temperature rise than would be preferred has been allowed in this design. The temperature rise will not be less than 60 degrees C. Some designers would have shortened the commutator by resorting to copper brushes. This, in the authors' opinion, is not advisable. The newer types of graphite brushes indicate very encouraging progress toward lower friction coefficients and I^2R contact losses; and this progress, when thoroughly substantiated by time tests, can gradually be followed up by decreased commutator lengths.

The design set forth below will serve to illustrate certain important points arising in connection with the calculation of machines of this type.

Dimensions and Data of 750-Kw. 250-volt 1500-R.P.M. C.C. Generator.

Dimensions in centimetres.

Number of poles		6
Speed in R.P.M.		1500
Voltage		250
Armature diameter (D)		76
Armature gross length (λ_g)		34
Armature overall length		63
Number of ventilating ducts		8
Width of each duct		1.0
Number of slots		162
Yoke external diameter		174
Yoke internal diameter		139
Width of yoke		50
Breadth of magnet core parallel to shaft		34
Breadth of magnet core at right angles to shaft		20
COMMUTATOR		
Diameter		45
Useful length		54
Total length		70
TECHNICAL DATA		
Flux per pole, megalines		6.32
Number of face conductors		324
Reactance voltage		15.5
Average volts per segment		18.5
Output coefficient		2.55
Armature peripheral speed	meters per second.	60
Commutator peripheral speed	meters per second.	35

In this design the output coefficient is much higher than that obtained in the Siemens machine. This is partly on account of the lower voltage, and also on account of the higher temperature rise for which the design was proportioned. In addition, greater facilities for ventilation are obtained, for with a 6-pole design a greater internal diameter is appropriate than with a 4-pole design.

In Fig. 322 the complete field for a 375-kw. 2500 R.P.M. 250-volt continuous current turbo-generator manufactured by the British

Westinghouse Company, is shown. Four distinct sets of field windings are employed:

(1) Shunt winding on the main poles.
(2) Series winding on the main poles.
(3) Series winding on the interpole cores.
(4) Deri winding distributed in tunnels beneath the main pole faces.

Fig. 322. — British Westinghouse Co.'s 375 kw. 2500 R.P.M. 250 volt continuous current turbo-dynamo with rotating portion removed.

In this machine the interpole shoe extends over the full length of the armature core.

In Fig. 323 is shown a 200-kw. 110-volt 2000 R.P.M. continuous current turbo-generator built by the British Westinghouse Co. Some

machines of this design are installed at the Savoy Hotel, London. This illustration is interesting on account of the special commutator construction, and the necessary brush gear to collect 2000 amperes

FIG. 323. — 200 kw. 2000 R.P.M. 110 volt continuous current turbo-dynamo built by the British Westinghouse Co.

per machine. Although extremely rigid the brush rocker and frame is of light construction throughout. Copper gauze brushes are employed, there being a set of 8 brushes for each pole.

Complete data of the 375 kw. machine illustrated in Fig. 322 has been published in an article entitled "Continuous Current Turbo-Generators," by Beyer.* The following specification has been compiled from Beyer's data:

SPECIFICATION OF A 375 KW. 2500 R.P.M. CONTINUOUS CURRENT TURBO-GENERATOR BUILT BY THE BRITISH WESTINGHOUSE COMPANY.

Rated Output (kw.)	375
Rated Speed (r.p.m.)	2500
Terminal Voltage	240
Current per Terminal	1600
Number of Poles (P)	4
Periodicity	83

ALL DIMENSIONS IN CENTIMETRES.

Armature.

Diameter at Air Gap (D)	56
Internal Diameter	30
Gross Core Length (λg)	30.5
Net Core Length (λn)	23.0
Number of Ventilating Ducts	6
Width of each Duct	0.8

Slots and Teeth.

Number of Slots	72
Number of Slots per Pole	18
Width of Slot	1.07
Depth of Slot	3.5
Minimum Width of Tooth	1.07

Armature Winding (4-Circuit).

Number of Face Conductors	144
Number of Conductors per Slot	2
Number of Components forming One Conductor	2
Cross Section of One Component	0.50 sq. cm.
Cross Section of One Conductor	1.0 sq. cm.
Dimensions of Bare Conductor	2 × (0.4 × 1.27)
Current per Conductor	400
Current Density	400
Number of Turns per Segment	1

* See *Electrical World*, Vol. 50, p. 964. *Elecktrotechnik und Maschinenbau* Heft 39, p. 743.

Commutator.

External Diameter	30.5
Total length of Segment	88
Number of Segments	72
Width of (Segment plus Insulation) at Periphery	1.33
Number of Shrink Rings	4
Cross Section of each Ring	4 × 7

Brushes.

Number of Spindles	4
Number of Brushes per Spindle (9 Metal and 9 Carbon)	9
Width of Brush	35
Length of Arc of Contact	16
Designation and Quality of Brush	Metal and Carbon Combined
Amperes per Set of Brushes	400
Amperes per Compound Brush	90
Amperes per sq. cm. of Contact Surfaces (of metal brushes)	17

Main Magnet Poles.

Diameter of Bore of Pole Face	58
Length Parallel to Shaft	30
Width at Right Angles to Shaft	16
Length of Pole Arc	29
Pole Pitch (τ)	44
Ratio Pole Arc to Pole Pitch	0.66
Radial Depth of Air Gap	1.0

Commutating Poles.

Length Parallel to Shaft	30
Width at Right Angles to Shaft	25

Main Compensating Winding.

Total number of Ampere Turns per Pole	8800
Current at Full Load	1600
Number of Turns per Pole	6
Number of Components forming One Conductor	2
Dimensions of One Component	5 × 0.8
Current Density in Amperes per sq. cm.	200

Magnetic Data.

Flux per pole in Armature (Full Load) (megalines)	4.1
Flux per pole in Yoke (Full Load) (megalines)	4.7
Flux Density Pole Core (kilolines)	10.4
Flux Density Yoke (kilolines)	8.0
Flux Density Armature (kilolines)	9.3

Magnetic Data. — Continued.

Flux Density Teeth (corrected) (kilolines)	15.2
Flux Density Air Gap (kilolines)	5.6
Total Number of Ampere Turns per Pole	5000
Flux in Commutating Pole Cores (megalines)	0.36
Flux Density in Commutating Pole	11.2

Losses and Efficiency.

Armature Iron Loss (Watts)	4240
Brush Friction Loss (Watts)	3170
Brush I^2R Loss (Watts)	1630
Armature I^2R Loss (Watts)	3830
Compensating Winding I^2R Loss (Watts)	1430
Commutating Pole Winding I^2R Loss (Watts)	400
Compound Winding I^2R Loss (Watts)	100
Shunt Excitation I^2R Loss (Watts)	540
Regulating Rheostat I^2R Loss (Watts)	230
Total Losses (including windage and bearing friction)	15,600
Output in Watts	375,000
Input in Watts	390,600
Electrical Efficiency (Full Load)	9620

Constants and Coefficients.

Armature Peripheral Speed (Metres per Sec.)	73
Commutator Peripheral Speed (Metres per Sec.)	40
Output Coefficient (ξ)	1.56
Flux per sq. cm. of Air Gap Surface (β)	3060
Armature Coils per cm. of Periphery (α)	328
Armature Ampere Turns per Pole	7200

Allgemeine Elektricitats Gesellschaft Machines.

A laminated field construction is employed for the A. E. G. high speed continuous current machines, the necessary windings being arranged in slots.

In general, three windings are used:

(1) Main shunt winding.
(2) Interpole tooth series winding.
(3) Deri compensating winding.

Where necessary, a compounding winding is added, although in many cases this is unnecessary, as the armature reaction with load is compensated for by the Deri winding.

In Fig. 324 a 20-kw. continuous current turbo-generator built by the A. E. G. is shown. The frame is of substantial construction. The

Fig. 324. — 20 kw. continuous current turbo-generator by the A. E. G

Fig. 325. — 100 kw. continuous current turbo-generator by the A. E. G

bearings are carried in an extension of the main yoke. In Fig. 325 a 100-kw. turbo-generator is shown. The set is provided with a separate exciter fixed on the outside of the main pedestal. Metal gauze brushes are employed, thus permitting of a commutator of reasonable dimensions. A wound field system is illustrated in Fig. 326. Continuous current turbo-generating sets are built by

FIG. 326.—Field system of an A. E. G. turbo-generator showing shunt coils and Deri winding in place.

the A. E. G. in capacities ranging from 50 kw. up to 750 kw. The speeds are as follows:

TABLE 72.

RATED OUTPUTS AND SPEEDS OF A. E. G. CONTINUOUS CURRENT TURBO-GENERATORS WITH METAL BRUSHES.

Rated Output in Kw.	Speed in R.P.M.
50–300	3000
500	2000
750	1500

All these machines have metal brushes. In addition to the above line of machines, a line employing carbon brushes is built in capacities

FIG. 327.—100 kw. 3000 R.M.P. 125 volt turbo-generator by the Rateau Turbine Co. of Chicago.

of from 2 kw. to 20 kw. These machines are made in the sizes shown in Table 73.

TABLE 73.

RATED OUTPUTS AND SPEEDS OF A. E. G. CONTINUOUS CURRENT TURBO-GENERATORS WITH CARBON BRUSHES.

Rated Output in Kw.	Volts.	Speed.
2	115	5000
5	65 and 115	4500
10	115	4000
15	65 and 115	4000
20	115	3600

FIG. 328. — 100 kw. turbo-generator by the Rateau Turbine Co. of Chicago.

GENERATOR BUILT BY THE RATEAU TURBINE COMPANY OF CHICAGO.

In Fig. 327 is shown a 100-kw. 3000 R.P.M. 125-volt turbo-dynamo, built by the above firm. The brush gear is supported at the bearing end by a bracket and at the armature end by the end cover of the machine. The bearings are of the oil ring type with a water jacket in the top and bottom cap. Commutating poles are employed in order to obtain satisfactory commutation. The machine has a nickel steel shaft, so designed that the critical speed is attained at a speed above the normal. The ventilation of the machine is assisted

by a fan which draws air in at one end of the armature and expels it at the opposite end.

The terminal board mounted on the machine is made sufficiently large to carry German silver shunts for adjusting the compound and interpole windings.

In Fig. 328 another view of the same machine is given. In this view the method of shifting the brushes is shown. This is accomplished by shifting the end plate by means of a hand wheel fixed to the outside frame.

Homopolar Generators.

A good deal of attention has been given in several quarters to the designing of the *homopolar* or *acyclic* type of generator for the high speeds associated with steam turbine driving.

In this type of generator the armature conductors are arranged to revolve in a unidirectional magnetic field. The emf. induced in the conductors is therefore unidirectional and does not, as in the ordinary type commutator generator, alternate as the armature revolves. Hence in this type of machine the commutator may be dispensed with and the commutator difficulties are eliminated.

The magnetic circuits and armature electric circuits may, in homopolar generators, be so disposed that the armature induced currents flow (1) radially or (2) axially with respect to the axis of rotation of the armature.

These two types may be designated, respectively, the radial type and the axial type. They are diagrammatically illustrated in Figs. 329 and 330, where the armatures consist, respectively, of radial disks with brushes at the inner and outer peripheries, and a cylindrical barrel with brushes at each end, for the collection of the currents.

In each of these cases the armatures consist virtually of an infinite number of circuits in parallel, and consequently, even at very high speeds, machines of this type are suitable only for low voltages and correspondingly heavy currents. In order to obtain moderately high voltages it is necessary to connect a number of conductors in series. Consequently in practice the continuous drum is replaced by a drum carrying a number of parallel conductors, and since the currents in all conductors are in the same direction, connection must be made

from the back end of one conductor to the front end of the other and the connections must lie outside the region of the magnetic field.

In order to connect the armature conductors in series, it becomes

Fig. 329. — Radial type of homopolar generator.

necessary to connect each end of each conductor to a slip ring with brushes to which the connecting leads are attached.

The principal difficulty with this type of machine relates to the

Fig. 330. — Axial type of homopolar dynamo.

large friction loss associated with the conduction of the current from the large number of slip rings required for obtaining reasonably high voltages.

FIG. 331. — Noeggerath's 500 kw. Homopolar Dynamo.

Such machines have been built commercially in moderately large sizes, by the General Electric Company of America. This company has recently produced a 500-kw. 600-volt homopolar generator.

An example of the type of machine built by this company is the 300-kw. 500-volt turbine-driven continuous current homopolar generator described by Noeggerath in a paper read before the American

FIG. 332.—Armature of 500 kw. Noeggerath Homopolar Generator.

Institution of Electrical Engineers. The following data is compiled from Noeggerath's description. In this machine the armature consists of twelve conductors of strip copper mounted on a cast steel drum. These conductors are connected to the twelve slip rings at either end of the armature. The twenty-four slip rings are made of cast steel. One copper brush is provided for each ring, and access to the brushes is obtained through the eight circular holes in the cast-steel frame as shown in Fig. 331. The armature is similiar to the one illsutrated in Fig. 332, which, however, relates to a 500 kw. homopolar generator. A view of the complete 500 kw. machine recently built by the General Electric Co. of Schenectady is shown in Fig. 331.

The speed of the 300 kw. machine is 3000 R.P.M., or 50 revolutions per second. The approximate dimensions of the armature are 49 centimetres diameter, and 31 centimetres effective length. This gives a peripheral speed of 77 metres per second. The volts per conductor $= \frac{500}{12} = 41.5$, the armature flux $= \frac{41.5 \times 10^{-8}}{50} = 83$

megalines. Therefore the flux density in the gap $= \dfrac{83}{31 \times 49 \times \pi} =$ 17,500 lines per square centimetre.

The twelve stationary conductors are connected to the brushes by leads mounted on a brush rocker, so that the magnetomotive force generated by the current flowing through these leads can be arranged to assist the magnetomotive force of the field coils. It is claimed that in this way the machine can be compounded positively for generators, or, in the case of motors, the compounding can be made negative. An efficiency curve of a 300 kw. Noeggerath generator is given in Fig. 333.

A strong point in the favour of the homopolar type of machine is the simplicity of construction and consequent low total cost of

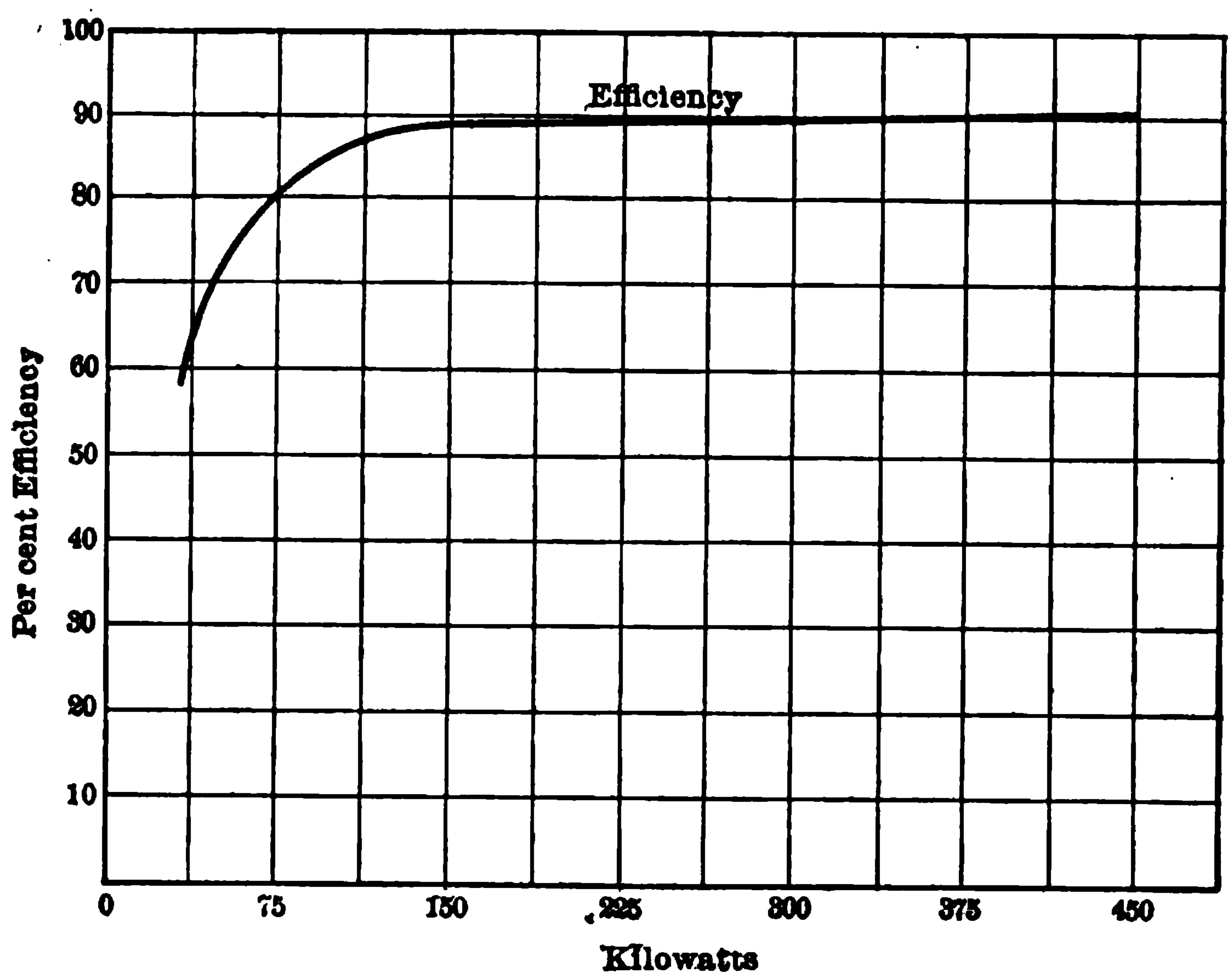


FIG. 333.—Efficiency curves of 300 kw. 500 volt homopolar dynamo.

manufacture. Pohl, however, observes in a paper on "The Development of Turbo Generators" read before the British Institution of Electrical Engineers on Nov. 22nd., 1907, that the chief objection to the homopolar type is the excessive weight of steel required. Pohl states that this is due to the low values for the armature ampere conductors per cm. of periphery, and gives as an example the case of 1000 kw. 600 volt generator at 1900 R.P.M. for which he estimates

that 51 tons of steel will be required for a homopolar type machine as against 5 tons for a commutating machine.

Since the homopolar generator in its simplest form is inherently a low voltage machine, it may find a sphere of usefulness for direct coupled exciters of turbo-alternators. In the latter machines the lower the voltage, the larger will be the cross section of the field conductor, and the fewer the number of turns required on each pole. This is advantageous in that a high space factor is obtained, and also that the field winding is better mechanically, which is especially important in rotating field systems.

The Westinghouse Company have constructed a homopolar generator for an exciter which generates at 10 volts, and is of the axial type. The field is radial, and the current passes along the shaft to the field circuits of the alternator.

Stresses in a Rotating Armature.

The chief stresses in a rotating armature may be conveniently tabulated as follows:

1. *Armature Body.* — Tension in the stampings.
2. *Slots and Teeth.* — Centrifugal force of the embedded copper; stress in the wedges; and tension in the teeth.
3. *End Windings.* — Stress in the binding wire or end covers.
4. *Commutator.* — Stress in the segments due to bending, and tension in shrink rings.
5. *Commutator Connections.* — Stresses due to vibration often causing crystallization and ultimate fracture.

The centrifugal force of a concentrated mass is given by the formula

$$F = 0.0000112\, WRN^2 \quad \ldots \ldots \ldots \quad (1)$$

where F is the centrifugal force in tons,
W is the *weight* of the mass, in tons,
R is the mean radius in centimetres,
N is the speed in revolutions per minute.

In the case of a ring, the mass is distributed over the whole circumference, and the component (F') tending to produce fracture across any section is given by

$$F' = \frac{F}{\pi} = 0.0000036\, WRN^2 \quad \ldots \ldots \quad (2)$$

Equation (1) may be used for obtaining the centrifugal force of any concentrated mass, and equation (2) for the force tending to produce fracture in a cylinder.

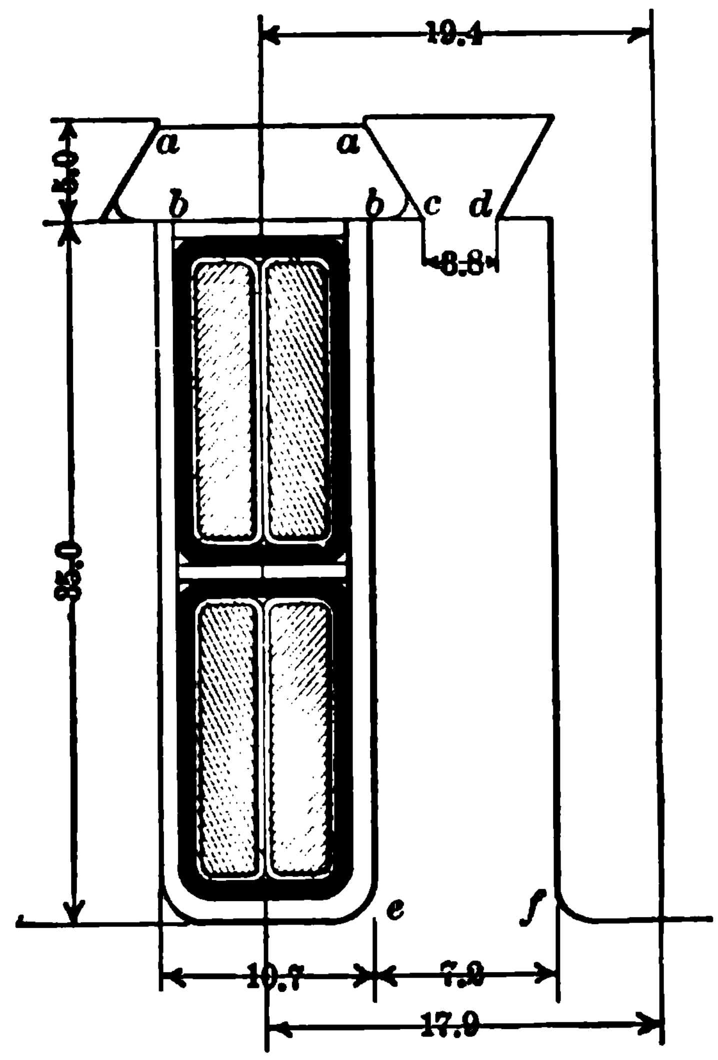


FIG. 334.—Armature slot for 1000 kw. continuous current generator.

The application of this may best be shown by means of an example. In Chapter XVII, page 398, is given the specification of a 1000-kw. 1000-volt 1000 R.P.M. continuous current generator, and an outline sketch of the armature slot is given in Fig. 334. The calculations for this machine may be carried out in the following manner:

1. *Armature Body.*—The maximum circumferential stress in the armature laminations occurs at the point where the diameter is greatest. In the armature body this will be at the roots of the teeth, that is, at the belt of iron immediately below the slots. The circumferential tension in a rotating cylinder is given by the expression

$$F = \frac{mv^2}{10},$$

where

F denotes the stress in kilograms per square centimetre,

m denotes the specific weight of the material in grams per cubic centimetre,

v denotes the peripheral speed in metres per second.

In the case of the 1000 kw. 1000 R.P.M. generator the external diameter of the armature laminations is 100 centimetres, and the diameter at the bottom of the slots 92 centimetres, which corresponds to a peripheral speed of 48 metres per second. The specific weight of iron is 7.8 grams per cubic centimetre, and hence we may obtain the stress in the stampings:

$$F = \frac{7.8 \times 48^2}{10} = 1800 \text{ kilograms per square centimetre}$$

or 0.18 tons per square centimetre.

This value is not excessive for wrought iron or sheet steel.

2. *Slots and Teeth.* — The cross section of one conductor is equal to 0.392 square centimetre, and there are 4 conductors per slot.

The total cross section of copper per slot is equal to

$$4 \times 0.392 = 1.57 \text{ square centimetres.}$$

The gross core length $(\lambda_g) = 50$ centimetres, therefore the total volume of copper embedded in one slot is equal to

$$1.57 \times 50 = 78.5 \text{ cubic centimetres,}$$

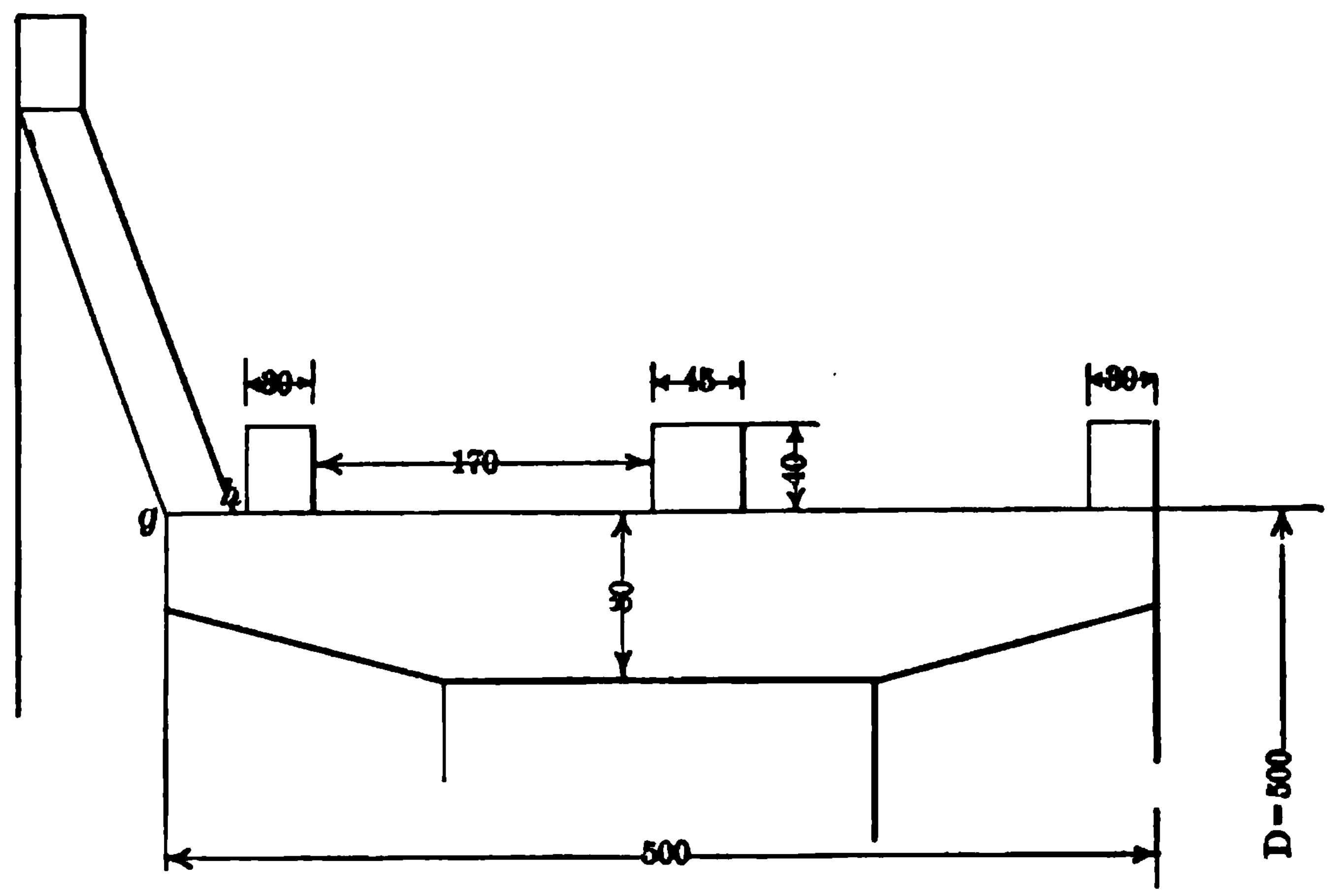


FIG. 335. — Commutator segment for 1000 kw. continuous current generator.

and the weight of copper per slot $= 78.5 \times 0.0089 = 0.70$ kilogram, or 0.00070 ton.

The external diameter of the laminations = 100 centimetres, and the diameter at the bottom of the slots = 92 centimetres.

$$\text{Therefore the mean diameter} = \frac{100 + 92}{2} = 96 \text{ centimetres,}$$

$$\text{and the mean radius} = \frac{96}{2} = 48 \text{ centimetres,}$$

and applying equation (1) the centrifugal force per slot amounts to

$$0.0000112 \times 0.00070 \times 48 \times (1000)^2 = 0.38 \text{ ton.}$$

This force sets up a shearing stress in the wedges, and the total section (ab) offering resistance to shearing is equal to twice the depth

of the wedge multiplied by its length. In this case the depth is 0.5 centimetre, therefore the total section

$$= 2 \times 0.5 \times 50 = 50 \text{ square centimetres,}$$

and the shear stress is equal to

$$\frac{0.38}{50} = 0.0076 \text{ ton}$$

This is rather a high value for wood, and a very hard wood should be employed, or else metal wedges.

The centrifugal force on the windings also sets up tension in the teeth. The slot pitch at the periphery is 1.94 centimetres.

The greatest width of wedge = 1.4 centimetres; therefore, the least width of tooth = 0.54 centimetre; and, since the net core length (λ_n) = 36, the least section (*cd*) in tension amounts to $36 \times 0.54 = 19.5$ square centimetres, and the corresponding stress $\frac{0.38}{19.5} = 0.0195$ ton per square centimetre.

Tension at the roots of the teeth is also caused by the centrifugal force both of the copper windings and of the teeth themselves. The weight of the teeth is 141 kilograms, therefore the weight per tooth $= \frac{141}{162} = 0.87$ kilogram, or 0.00087 ton, and the centrifugal force (F) $= 0.0000112 \times 0.00087 \times 48 \times (1000)^2 = 0.47$ ton. The centrifugal force on the copper was found equal to 0.38. Therefore the total force on the roots of one tooth is

$$(0.47 + 0.38) = 0.85 \text{ ton.}$$

The slot pitch at the root is

$$\frac{92\pi}{162} = 1.79 \text{ centimetres.}$$

The width of the slot is 1.07, and the width of the tooth at the root is $(1.79 - 1.07) = 0.72$ centimetre, and the total section resisting fracture (*ef*) $= 0.72 \times 36 = 26$ square centimetres.

Hence the stress amounts to

$$\frac{0.85}{26} = 0.0326 \text{ ton per square centimetre.}$$

3. *End Windings.* — The total weight of the armature copper is

260 kilograms, and of this amount 0.7 kilogram per slot, or (0.7 × 162 = 113 kilograms for all the slots, is restrained by the slot wedges. The remaining (260 — 113) or 147 kilograms is restrained by binding wire.

From equation (2),

$$F' = 0.0000036 \times 0.147 \times 48 \times (1000)^2 = 25 \text{ tons.}$$

Allowing a stress of 0.7 ton per square centimetre, the required section would be $\frac{25}{0.7} = 36$ square centimetres.

Using a steel wire of 0.2 centimetre diameter, the section per turn

$$= \frac{\pi\,(0.2)^2}{4} = 0.0314 \text{ square centimetre,}$$

and the number of turns $= \dfrac{36}{0.0314} = 115.$

That is, we might use 60 turns at each end of the armature, and wind in a double layer. It must not be forgotten, however, that the value 0.7 ton per square centimetre does not represent the total resultant stress, as this figure does not include the amount due to the centrifugal force of the wire itself.

This may be conveniently calculated by considering such a number of wires that the cross section is equal to 1 square centimetre.

The volume of these wires would be 100π, and the weight

$$100\pi \times 0.0078 = 2.45 \text{ kilograms, or } 0.00245 \text{ ton.}$$

The centrifugal force is then equal to

$$0.0000036 \times 0.00245 \times 50 \times (1000)^2 = 0.44 \text{ ton.}$$

The total section resisting this force is 2 square centimetres, and hence the stress is equal to $\frac{0.44}{2} = 0.22$ ton per square centimetre.

Hence the total stress on the binding wire is equal to

$$0.7 + 0.22 = 0.92 \text{ ton per square centimetre.}$$

Commutator.

Diameter of commutator	= 50 centimetres.
Radial depth of segments	= 8 centimetres.
Therefore, internal diameter	= 34 centimetres.

Hence neglecting insulation, the volume of copper per centimetre length of commutator

$$= \frac{\pi \times (50)^2}{4} - \frac{\pi \times (34)^2}{4} = 1960 - 900 = 1060 \text{ cubic centimetres,}$$

and the weight per centimetre length

$$= 1060 \times 0.0089 = 9.5 \text{ kilograms, or } 0.0095 \text{ ton.}$$

The mean radius is $\frac{50 + 34}{4} = 21$ centimetres, and the centrifugal force (F') is equal to

$0.0000036 \times 0.0095 \times 21 \times (1000)^2 = 0.71$ ton per centimetre length.

The total length of the commutator = 50 centimetres. Therefore the total force due to the commutator bars

$$= 50 \times 0.71 = 35 \text{ tons.}$$

In addition to this must be taken the centrifugal force on the commutator risers. If we take the section of these as

$$3 \times 0.2 \text{ centimetre} = 0.6 \text{ square centimetre}$$

the length is equal to 25 centimetres, therefore the volume = 15 cubic centimetres, and the weight per riser

$$= (15 \times 0.0089) \text{ kilogram} = 0.134 \text{ kilogram.}$$

There are 324 segments and therefore 324 risers. Therefore the total weight is equal to

$$(324 \times 0.134) \text{ kilogram} = 43.5 \text{ kilogram or } 0.0435 \text{ ton.}$$

Taking the mean radius as 35 centimetres, the centrifugal force (F')

$$= 0.0000036 \times 0.0435 \times 35 \times (1000)^2 = 5.4 \text{ tons.}$$

There are three steel rings; the middle ring is 4.5 centimetres wide, and the end rings are 3 centimetres wide. Therefore the total width is equal to $4.5 + (2 \times 3) = 10.5$ centimetres.

The radial depth of all the rings is 4 centimetres, from which we find the weight of all the rings to be 0.0635 ton.

The mean radius may be taken as 27 centimetres, hence the centrifugal force due to the rings themselves is equal to

$$0.0000036 \times 0.0635 \times 27 \times (1000)^2 = 6.1 \text{ tons.}$$

From this we obtain the total force tending to fracture the rings, due to the segments, the risers, and the rings themselves; this is equal to

$$35 + 5.4 + 6.1 = 46.5 \text{ tons.}$$

The total section of all the rings resisting this force is equal to

$$(2 \times 4 \times 10.5) = 84 \text{ square centimetres.}$$

Hence the average stress in the rings is equal to

$$\frac{46.5}{84} = 0.55 \text{ ton per square centimetre.}$$

We must also consider the tension at the roots of the risers themselves. Each riser weighs 0.000134 ton. Therefore the centrifugal force

$$= 0.0000112 \times 0.000134 \times 35 \times (1000)^2 = 0.0505 \text{ ton.}$$

But the section of each riser (gh) is 0.6 square centimetre. Therefore the stress amounts to 0.084 ton per square centimetre.

Lastly we must consider the bending in the commutator segments. In this case the stress at a point half way between the rings is given by the expression

$$f = \frac{0.75\, Fl^2}{bd^2},$$

where F is the centrifugal force in tons per centimetre length of a single commutator segment,
l is the distance between the shrink rings in centimetres,
b is the average breadth of the commutator segment in centimetres,
d is the depth of the segment in centimetres, and
f is the stress in tons per square centimetre.

The weight of commutator per centimetre length = 0.0095 ton. Therefore the weight per bar per centimetre length

$$= \frac{0.0095}{324} = 0.0000292 \text{ ton.}$$

Therefore the centrifugal force (F) per centimetre length per bar = $0.0000112 \times 0.0000292 \times 35 \times (1000)^2 = 0.0115$ ton.

The distance between the shrink rings (1) is equal to 17 centimetres, and the depth of the segment (d) is equal to 8 centimetres. The average breadth of a segment (b) is given by the expression

$$\frac{0.415 + 0.28}{2} = 0.35 \text{ centimetre.}$$

Hence

$$f = \frac{0.75 \times 0.0115 \times 17^2}{0.35 \times 8^2} = 0.112 \text{ ton per square centimetre.}$$

CHAPTER XIX.

BRUSHES AND BRUSH GEAR FOR HIGH SPEED CONTINUOUS CURRENT DYNAMOS.

We have shown that with high speed continuous current dynamos, special provision for commutation should be made by the addition of compensating windings or interpoles. The function of carbon brushes as an essential factor in commutation may in these cases often be of considerably less importance, and the question arises as to whether carbon brushes may not be replaced by metallic brushes. Although metallic brushes of various forms are considerably used, they are not as yet superseding carbon brushes to so great an extent as, from this point of view, one would be led to expect.

There is a tendency to lose sight of the fact that the superiority of carbon as a material for dynamo brushes has been due by no means exclusively to its relatively high resistance, and its consequent usefulness in connection with the sparkless collection of the current, but has to a great extent been due to certain unique mechanical attributes. Whereas copper brushes of the type heretofore usual, have frayed at the edges, have become clogged up with dust, have tended to cut the commutator surface, and have in various other respects proved distinctly unsatisfactory; brushes of carbon have, in the great majority of instances where they have been employed in well-designed and carefully constructed machines, proved themselves to be quite free from these troubles. These statements hold good up to commutator peripheral speeds of some 15 metres per second, and often for considerably higher speeds; and the increased surface of commutator required, owing to the relatively low current density and high resistance of the contacts in the case of carbon brushes, has been repeatedly demonstrated to be a justifiable initial extravagance owing to the lower maintenance costs and the greater freedom from interruption of service which have thereby been secured.

Even before the advent of high speed continuous current dynamos, it was frequently contended that in the very large commutator

necessarily entailed by the use of carbon brushes, the cost of the machine was seriously increased. Nevertheless, and, as the authors believe, rightly, the advantages of carbon brushes, which are not only electrical but mechanical, have generally been considered so great as to justify the large commutators which they entail.

The present demand for very high speed continuous current dynamos has introduced a new factor. The requirements of such designs often render it impracticable to employ peripheral speed, of less than from 25 to 35 metres per second, and at such speeds the ordinary carbon brush does not operate satisfactorily from the mechanical standpoint.

Some brands of graphite brush have given somewhat better satisfaction from the mechanical standpoint at these high peripheral speeds; and it appears probable that good mechanical results will in the near future be obtained by the use of suitably constructed metal brushes. While, however, these new conditions have stimulated designers and manufacturers to the production of improved metal brushes, it is still a question whether the results which have as yet been obtained by the latest types can be termed satisfactory. There is still a great deal of room for the improvements which will doubtless be forthcoming, but whether these will lead us to graphite or to copper brushes, or to some other material, or to some combination of these two or to some other two or more materials, it is difficult to foretell.

It appears that certain of the newer graphite brushes may be used at current densities well on toward the values which it would be desirable to employ even with metal brushes, so that while at high speeds the metal brush appears to be approaching the carbon brush as regards mechanical advantages, the carbon brush — or, more precisely, the graphite brush — appears to be approaching within reasonable distance of the metal brush as regards permissible current densities. Both types of brush are relieved to a certain extent from the third requirement of contributing to the sparkless collection of the current, by the developments in the matter of interpoles and compensating windings. Carbon brushes have so strongly established themselves in favour by many years of extensive use, that it will require a vigorous campaign in favour of some metal alternative, even of considerable superiority, to accomplish its widespread introduction.

The question of brushes for high speed dynamos has received

considerable attention in several papers and discussions relating to turbines and turbo-generators, and the following references are of interest in this connection:

In a paper by Messrs. Parsons, Stoney, and Martin,* entitled "The Steam Turbine as applied to Electrical Engineering," there occurs the following passage: "Carbon brush blocks cannot be used, as at these speeds the brushes are apt to vibrate, and so diminish the intimacy of contact and cause heating and undue wear. The result is that it has been found best to form the brushes of wire gauze or foil, preferably of brass, and these brushes must be sufficiently flexible to maintain a good contact with the commutator over the whole section of the brush."

In the discussion on this paper, W. B. Sayers stated that metal brushes were operated quite satisfactorily on machines provided with the Sayers compensating winding. It was also stated that carbon brushes were quite practicable on turbo-generators if the amount of subdivision of the brushes was great, and if each separate block was so mounted as to have a very small amount of inertia. W. J. London, in a paper entitled "Steam Turbines and Turbo-Generators," † says: "It has been found that it is almost impossible to use carbon brushes on this type of generator, owing to the fact that the armature floats when running, thus causing the brushes to dance. Metal brushes must, therefore, always be used, and in consequence, unless satisfactory commutating poles or compensating windings are adopted, the position of the brushes must be altered to suit small variations in load."

The latter plan was employed by Parsons in his early continuous current machines. The apparatus consisted of an automatic brush shifting gear. The brush rocker was connected to a small piston, which was subjected to steam from behind the turbine poppet valve the pressure of which was found to be sufficiently proportional to the load. The arrangement was adjusted so as automatically to give to the brushes an amount of lead proportional to the load. It has been stated that good results were obtained.

The method has been abandoned by Messrs. Parsons in favour of a compensating winding designed by Messrs. Parsons and Stoney. This winding is placed in slots in and between the pole faces, and is

* Vol. 33, p. 808, *Journal Institution Electrical Engineers*, England.

† Vol. 35, p. 185, *Journal Institution Electrical Engineers*.

distributed completely around the periphery of the armature. With metal brushes, the contact resistance is about one eighth of that of ordinary carbon brushes, and the permissible current density is stated to be 20 to 30 amperes per square centimetre (as against 4 to 6 amperes per square centimetre for carbon brushes) without occasioning excessive heating at the contacts. This means that the brush contact area need be only one fifth of what it would be for ordinary carbon brushes, and a considerable reduction in the length or diameter of the commutator is permissible. Since in high speed machines, the commutator assumes very large proportions compared with the armature, and since problems relating to centrifugal forces of the segments are involved, the shortening of the commutator made practicable by metal brushes is a strong point in their favour, provided that a satisfactory performance in other respects can be obtained. The voltage drop and the loss in the brushes are also considerably reduced by metal brushes, and an increase in efficiency of from 1 per cent to 3 per cent or more (according to the voltage of the machine) may result. This is the more important in view of the extra loss in the compensating windings. The friction coefficient for metal brushes is only some two thirds of that for carbon brushes.

This commutation problem is very serious in all high speed continuous current machines in spite of the assistance afforded by interpoles and compensating windings. Hence the final solution of the brush problem should combine the advantages of the copper brush with the good commutating properties and, in some respects, better mechanical properties, of the carbon brush. A marked tendency is becoming apparent towards some form of a compound brush which shall combine good conductivity with a high resistance to the transverse currents from the short circuited coils. Before discussing these still problematical tendencies, it is advisable to review the general properties of the ordinary carbon brush.

General Properties of Carbon Brushes. — The investigation of the voltage drop and contact resistance with varying current density, pressure, speed, and quality of carbon, has formed the subject of a large amount of theoretical and experimental study. Nevertheless, it is still somewhat difficult to harmonise the results which have been obtained by the various investigators.

The quality of the carbon, as regards hardness, specific resistance, and other physical properties, greatly affects the results obtained

from a series of tests, even when carried out under identical conditions. The variations introduced by the use of different types of brush holders are alone sufficient to insure discordant results. Nevertheless, analyses of the results of the various investigations disclose definite tendencies and lead to conclusions which are at any rate *qualitatively correct*, and from which *rough* but nevertheless useful *quantitative data* may be compiled.

Contact Resistance and Current Density.—The contact resistance between a brush and a rotating ring or commutator varies

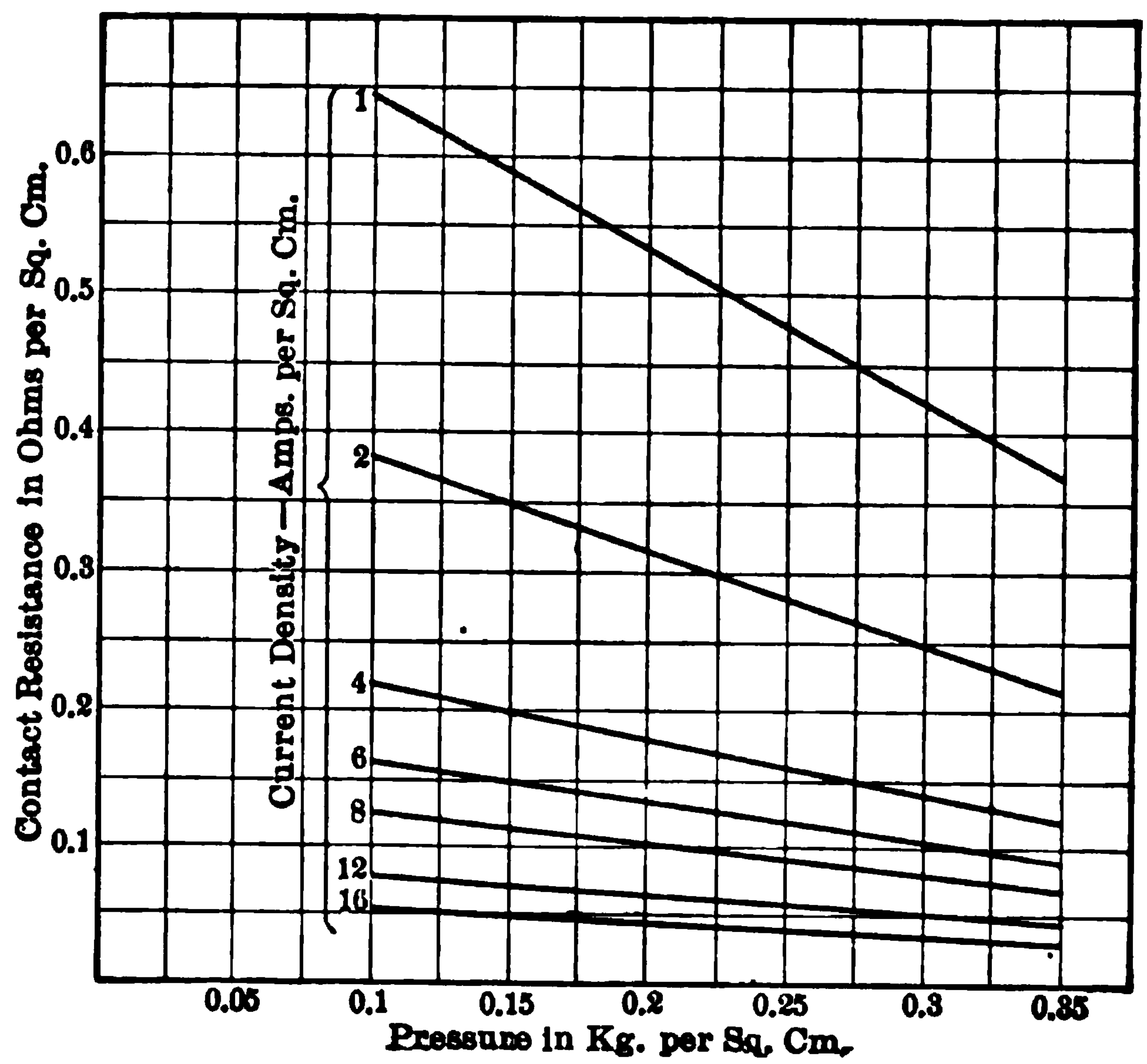


FIG. 336. — Variation of contact resistance with brush pressure for a soft carbon.

widely with the current density. The general relation tends to conform to hyperbolic curves such as those shown on page 473, for a particular make of brush in Fig. 343. It is seen that for all except very low current densities, the voltage across the contact is quite constant for any particular pressure and is fairly independent of the current density.

Contact Resistance and Pressure.—Within certain limits the contact resistance may be decreased by increasing the contact pressure. This is shown in Fig. 336, where the contact resistance for a soft carbon at several different current densities is plotted against

the pressure. These particular curves are deduced from those of Fig. 343 and relate to a soft graphite brush.

In Fig. 337 are shown similar curves for a fairly hard brush, suitable for a street railway motor, where rather heavy pressures are necessary in order to insure good contact, in spite of the vibration of the car. In both of these figures it is evident that at light pressures a change in pressure considerably affects the contact resistance, but that for a hard brush, or at any rate for the grade of brush corresponding to Fig. 337, the contact resistance at rather high pressures is nearly or quite independent of the pressure. The degree of depend-

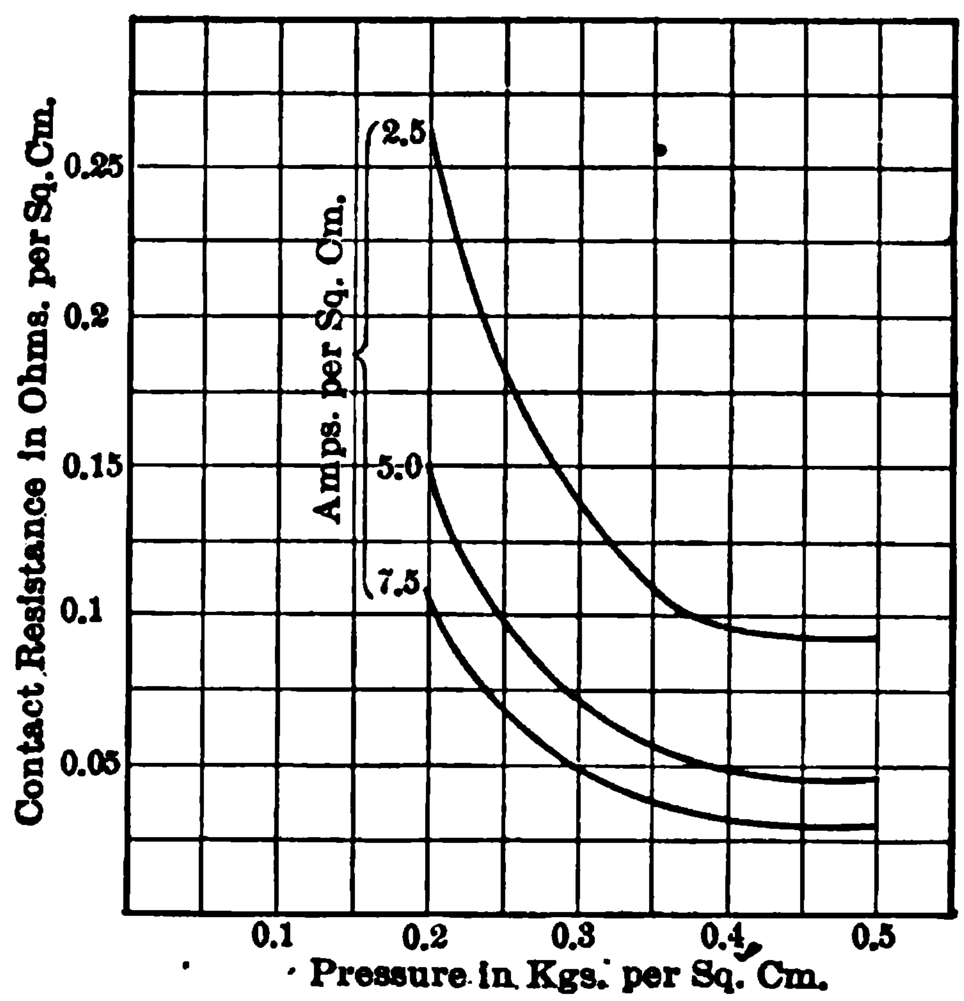


FIG. 337. — Variation of contact resistance with brush pressure for a hard brush employed in service where heavy pressure is required.

ence on the pressure varies considerably with different grades of carbon, and extensive tests of any particular grade of carbon are necessary in order to obtain even approximate values for that grade.

In the case of two qualities of carbon manufactured by the "Le Carbone" Company and designated as grades "X" and "Z," the contact resistance is stated to be independent of the pressure between the limits of 0.15 and 0.20 kilogram per square centimetre.

Contact Resistance and Peripheral Speed. — For a soft carbon of the Le Carbone Company, some observations of the effect of the peripheral speed on the contact resistance are plotted in the full line curves of Fig. 338.

The dotted line curves in the same figure represent a very hard carbon, from which it is seen that the hard carbons have a considerably higher contact resistance than the soft carbons, and that with the hard carbons the contact resistance attains a maximum value at a comparatively low speed, and that any further increase in speed produces little or no change in the contact resistance. The curves in Fig. 338 are deduced from results given by Dr. S. P. Thompson, in his 1906 Howard Lectures on "High Speed Electric Machinery with special reference to Steam-Turbine Machines."* The dotted curves are stated by Dr. Thompson to represent data originally due to Lessing.

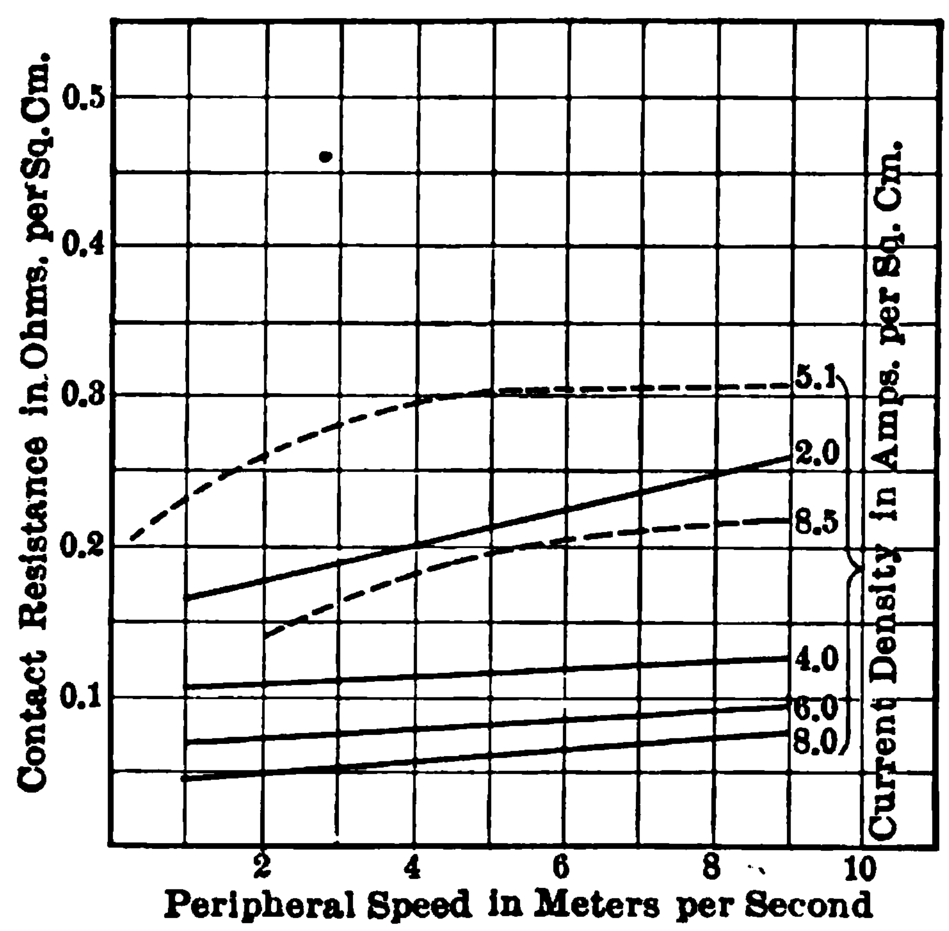


FIG. 338. — Variation of contact resistance with peripheral speed of commutator.

Contact Resistance and Quality of Carbon. — As mentioned in the previous paragraph, the contact resistance is very much higher for the harder carbons, ranging up to values two or three times higher than those obtaining for the soft carbons under the same conditions.

It has also been shown that the effect of alterations in the peripheral speed and in the pressure, is to produce results which differ both in magnitude and form according as the carbon is hard or soft; this effect is very irregular, and for different grades of carbon, often produces results of a very contradictory and irreconcilable nature.

Voltage Drop. — Owing to the tending toward hyperbolic shape manifested by the curves representing contact resistance when plotted

* Delivered before the Society of Arts, London, on January 18 and 25, and February 1, 1906.

as a function of the current density, the voltage drop under widely different conditions does not vary to any great extent. Its magnitude is greatly affected by the quality of the carbon, and particularly by the degree of hardness. A curve is shown in Fig. 343 representing the voltage drop for different current densities for a soft carbon.

For purposes of estimating the commutator losses in a given design, it is usually sufficiently accurate to use approximate values of the voltage drop as given in Table 74 which represents the sum of the fall of potential at both the positive and the negative brushes.

TABLE 74.

VOLTAGE DROP AT BRUSH CONTACTS.

Quality of Carbon.	Voltage Drop (Positive plus Negative).
Very hard	2.4 to 3
Hard	2 to 2.4
Soft	1.4 to 2
Very soft.	0.9 to 1.4

The values in Table 74 may be taken as roughly representative of modern practice for a fairly wide range of working conditions. Broadly speaking, the softer the carbon, the higher will be its conductivity, and hence the current density may be higher. The working values vary from about 4 to 12 amperes per square centimetre; though more commonly they are within the limits of 5 to 8 amperes per square centimetre. *For the ordinary grades of carbon brush, it is desirable to keep down to the lower values of* 5 *amperes per square centimetre.* For the working pressure, 0.1 kilogram per square centimetre may be used with advantage for the softer carbons, but the hard carbons may require 0.20 or 0.25 kilogram per square centimetre; and where the machine is subject to considerable vibration, as in the case of tramway motors, the necessary pressure may be even double this amount.

Commutator Losses. — The two chief sources of loss in the commutator are those of contact resistance and friction.

Contact Resistance Losses. — The I^2R loss may most readily be estimated as the product of the voltage drop and the current. Designers almost always use roughly representative values, such as those given in Table 74.

Friction Losses. — The friction losses at the commutator are directly proportional to:

1. The peripheral speed of the commutator.
2. The total pressure on all the brushes.
3. The coefficient of friction between the brushes and the surface of the commutator.

In high speed machines, the friction losses are generally considerably higher than the losses due to the contact resistance, so that designers should aim to keep the diameter as small as is possible without making the commutator excessively long, and thus involving both increased cost for the machine and also undue mechanical stresses.

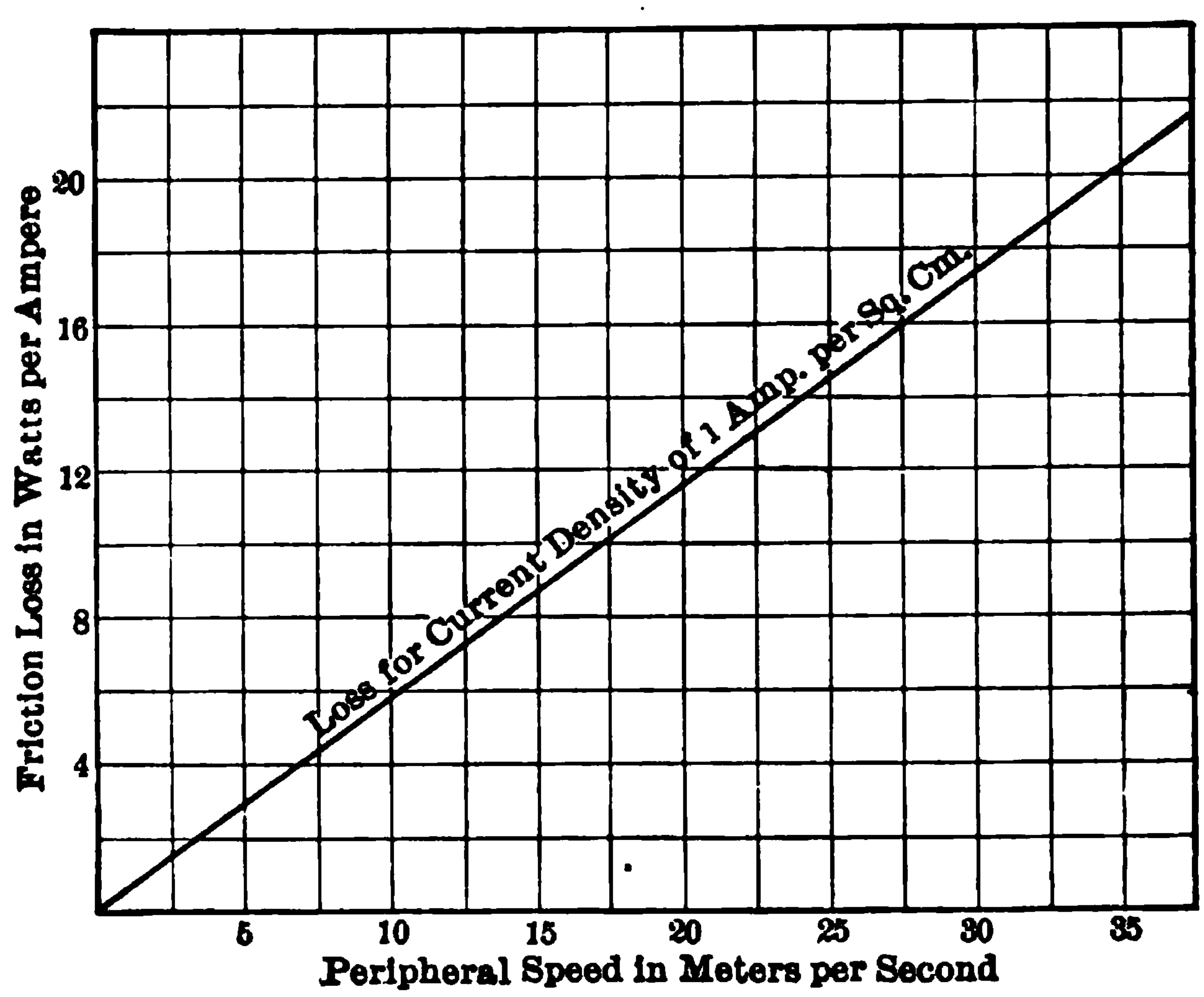


FIG. 339. — Curves for establishing brush friction loss at commutator for a brush pressure of 0.1 kg. per sq. cm.

The total pressure on all the brushes may be expressed as the product of the pressure per square centimetre, and the total contact surface. The contact surface is a function of the current density and of the current to be collected. Hence, for carbon brushes at a given pressure, and having a given coefficient of friction, a curve may be drawn to represent the friction losses in watts per ampere for unity current density, at various peripheral speeds.

Fig. 339 shows such a curve calculated for a friction coefficient of 0.3 and a pressure of 0.1 kilogram per square centimetre.

The total friction loss is obtained by dividing the loss for a current density of one ampere per square centimetre, by the current density, and multiplying the result by the total current. The losses for any other coefficient of friction can be obtained by direct proportion. Thus for a class of brush with a friction coefficient of 0.2 instead of 0.3, the losses will be two thirds of the amount indicated by the curve in Fig. 339. Similarly, as the friction loss is proportional to the pressure, the loss for a pressure of, say, 0.2 kilogram per square centimetre is twice the value obtained from the curve.

Effect of Speed on the Dimensions of the Commutator.

In order to illustrate the contrast between the proportions of commutators of low speed machines and those of high speed machines, the leading dimensions of the commutators of two 500-kw. 500-volt machines, running at 125 and 2500 R.P.M. respectively, are set forth in the following table. A complete specification of these two machines has been given on page 367 of Chapter XV.

DIMENSIONS OF COMMUTATORS OF 500 KW. 500 VOLT MACHINES FOR 125 AND 2500 REVOLUTIONS PER MINUTE.

	Speed in R.P.M.	
	125.	2500.
Diameter of commutator . . . . cm.	200	27
Length of segment . . . . cm.	13.5	66
Number of segments	1200	64
Width of (segment + insulation at periphery) . . cm.	0.53	1.3
Current strength of machine . . . . amps.	1000	1000
Current density at contact surface . amps. per sq. cm.	7	7
Volts drop at brush contacts (positive plus negative)	2	2
I^2R loss at brush contacts . . . . watts	2000	2000
Peripheral speed in metres per second	13	35
Friction loss in watts per ampere	1.2	2.5
Total friction loss . . . . watts	1200	2500
Total commutator loss . . . . watts	3200	4500
Cylindrical surface in sq. dm.	85	56
Watts per sq. dm.	38	80

From this table the most striking contrast is observed, as regards the ratio of the diameter of the commutator to its length. Whereas the I^2R loss may be taken at the same value for both machines, the friction loss in the high speed machine is double and the watts

per square decimetre of the commutator surface reach more than twice the value found for the low speed machine.

In spite of the tendency towards better cooling due to the higher peripheral speed of the 2500 R.P.M. machine, it is difficult or impossible to make such ventilating provision as would bring about so small a rise in temperature for the 2500 R.P.M. machine as that which could be obtained in the 125 R.P.M. machine.

Choice of Carbon Brushes. — For low speed machines, the friction losses do not assume very great importance, so that a high coefficient of friction is not particularly disadvantageous. But where high peripheral speeds cannot be avoided, the friction losses require to be kept as low as possible. Hence, soft carbons are preferable from the point of view of both low voltage drop and low friction losses. The limitations, however, are both mechanical and electrical, for not only do some kinds of soft carbon brushes wear away much more rapidly (and this is particularly evident at high speeds), but a fine deposit from the brushes remains on the commutator, and may occasion sparking troubles. For many cases, another limitation to the soft brush is the very feature of its higher conductivity, for it is essential for machines having a high reactance voltage, to have brushes of a high resistance to assist the commutation. Hence resort to high conductivity brushes with a view to obtaining the advantages of less surface, and of less coefficient of friction for a given pressure over that surface, is not always practicable in high speed designs.

The design and adjustment of the brush holder has a very important bearing on the question of commutation at high speeds. It frequently occurs that brushes and brush holders which have given every satisfaction on a large number of machines, prove utter failures in certain individual cases.

In an article published in *Elektrotechnik und Maschinenbau* * it is pointed out by Molnar that brushes made of anti-friction metal or of any other alloy containing tin, should be avoided, as at the higher temperatures, the tin may spread over the commutator surface in the form of an oxide, thereby greatly increasing the contact resistance.

Molnar describes the process of manufacture of certain grades of carbon; the materials generally used are coal, petroleum coke, lampblack, and the remains of arc lamp carbons. These, or mixtures of them, are ground to a fine powder, are mixed with a suitable bind-

* Vol. 24, pp. 842–846, Oct. 21, 1906.

ing medium (mainly tar), pressed into moulds, packed into crucibles with carbon dust, and subjected to the heat of a furnace. The carbon as originally employed has but slight conductivity. In the doughy condition in which it is pressed into the moulds it has neither strength nor hardness. It does not acquire electrical conductivity nor mechanical strength until subjected to the baking process, and the degree to which these attributes are acquired, is greater, the higher the temperature to which it is subjected. Good conductivity is obtained as the result of the partial change of the carbon into graphite during the annealing process. Brushes prepared in this way are hard, and the hardness is greater, the greater the proportion of coal or coke used in the original mixture. Softer carbons are obtained by the addition of graphite, and the degree of softness is greater, the greater the amount of graphite added.

Graphite as mined from the earth is stated to rarely contain more than 50 per cent carbon, and it is very expensive to separate this from the equal quantity of earthy and organic impurities. Graphite as obtained by means of the electric furnace is practically pure carbon. According to the Le Carbone Company's process of converting the carbon into graphite, the customary annealing is followed by a process of placing the brushes in a special oven, and there subjecting them to the heat of the electric arc in an atmosphere of some neutral gas. The quality of the graphite is considerably improved by this process.

The brush is next ground to the required shape, and its posterior surface is coppered, nickelled, or silvered, in order to improve the contact with the brush holder. In some cases the brushes are impregnated with vaseline, oil, or paraffin, in order to render them self-lubricating, and thus to reduce the friction loss. In these brushes it is very important to have the lubricating medium entirely free from acids, and for this reason vaseline is the most suitable lubricant; paraffin is not to be recommended. Molnar, from whose article we have abstracted the preceding paragraphs, goes on to state that good carbon brushes must be very dense and homogeneous. Porous brushes are liable to crumble at high current densities or when subjected to mechanical vibration. Hard and coarse brushes are apt to cut the commutator and also to disintegrate at high peripheral speeds.

Molnar finds that brushes constructed of alternate layers of metal

and carbon are liable to cleavage, owing to the difference in the expansion coefficients. Brushes consisting of a mixture of metal dust and carbon have generally been found unsuitable for use with commutators, owing partly to their weight and partly to their unfavourable commutating qualities. They are, however, to be recommended for use on slip rings, as they are capable of standing higher current densities than ordinary carbon brushes.

Attempts have been made to construct brushes from several graphite slabs, each component slab having such composition as to endow it with suitable conductivity. These component slabs are laid upon one another with suitable binding material interposed, and are baked together into a compound brush. By this means the forward edge of a brush may have a suitably high resistance, and this may be graded until at the heel the resistance is much lower.

While most manufacturers have employed processes in which either carbon is more or less transformed into graphite, or else a proportion of graphite is added at a certain stage of the process, other manufacturers have employed various proportions of graphite and clay for the purpose of obtaining the desired conductivity. The graphite and clay are mixed into a plastic mass, which is then formed and baked. This type of brush is generally proportioned for rather high resistance, and hence have arisen confusingly divergent statements with regard to the conductivity of graphite brushes. Prepared by the first described processes, graphite brushes generally have much higher conductivity than ordinary carbon brushes, whereas when prepared by the last mentioned process, their conductivity may be much lower than that of ordinary carbon brushes.

Bailey and Cleghorne* have recently investigated the question of contact resistance. After obtaining some curves showing the relation between current density, voltage drop, peripheral speed, and total losses, they investigated the consequences of using paraffin wax as a lubricant. By this means the friction losses were decreased to about one fifth, without materially increasing the voltage drop. The drop, however, varied considerably with the speed, and when the ring was stationary, the resistance was very irregular, and often rose to what was practically total insulation.

The curves shown in Fig. 340 are due to Noeggerath; they show

* *Electrician*, Vol. 58, pp. 202–204, Nov. 23, 1906.

the voltage drop between copper brushes and a cast steel ring for a peripheral speed of 50 metres per second, and also when the ring is stationary. The curves in Fig. 341 show the losses and the temperature rise in the twenty-four slip rings of the homopolar machine

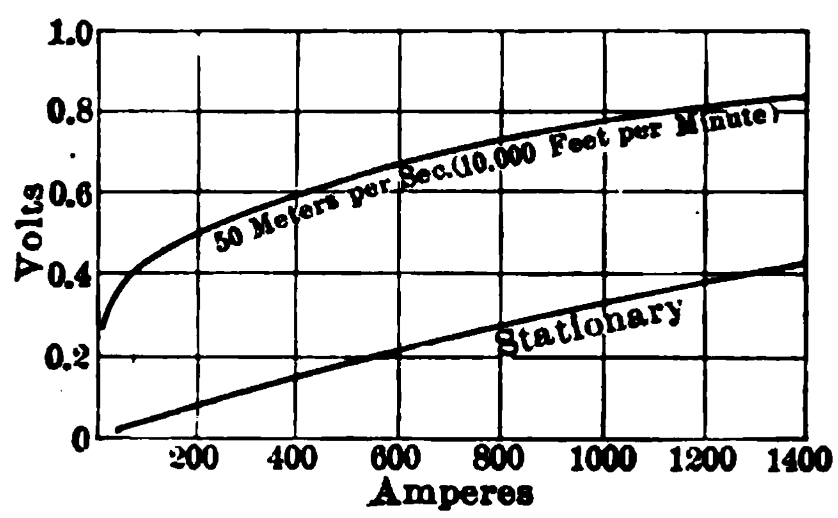


FIG. 340. — Noeggerath's tests of voltage drop between copper brushes and a cast steel ring.

described on page 458 of Chapter XVIII. Noeggerath points out that pressures from ten to twenty times higher than normal were often required to start the flow of current. The increase of brush pressure does not decrease the voltage drop to any great extent, but the

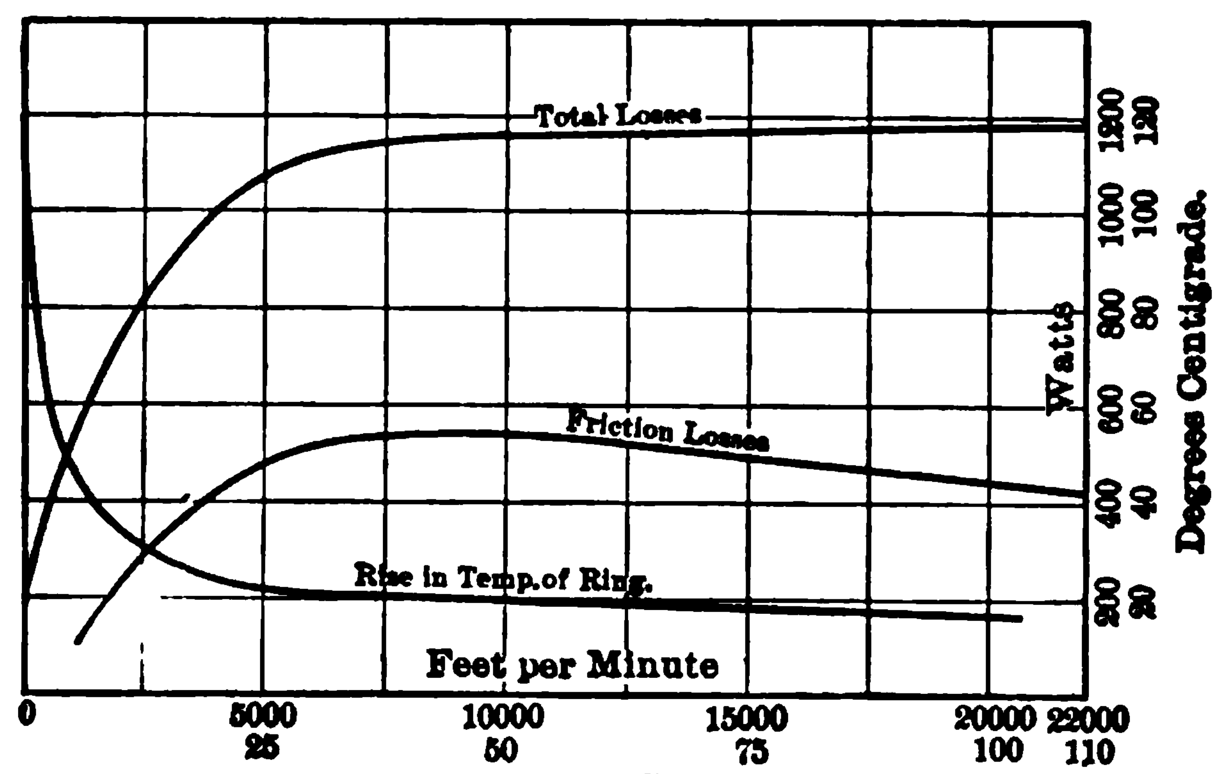


FIG. 341. — Total losses in the steel slip rings of Noeggerath's 300 k.w. homopolar machine.

sparking tendency at high speeds and densities necessitates high pressures.

Compound Brushes. — Before considering the question of compound brushes, it is well to summarise the chief advantages and disadvantages of the carbon brush.

Carbon brushes have a much lower conductivity than copper, the ratio for the contact resistance being about ten to one; furthermore the coefficient of friction is somewhat higher for the carbon brush. The coefficient may be taken at 0.2 for copper brushes as against from 0.2 to 0.3 for carbon brushes. Thus the commutator losses, as we have already pointed out, are very largely increased by the employment of carbon brushes.

FIG. 342. — Burleigh's construction for compound brushes.

Several proposals have been put forward with the object of obtaining the advantage of high conductivity without the objectionable feature of a low transverse resistance. The first step in this direction was to use laminated brushes. It has repeatedly been suggested that brushes might be built up of alternate layers of copper and insulation, laminated normally to the direction of rotation of the commutator. An interesting design on these lines is that devised by Burleigh, and shown in Fig. 342.*

Somewhat the same principle is employed in a brush made by the National Carbon Company. This is a carbon brush interwoven with

* *Electrical Review*, Vol. 54, page 940.

sheets of copper gauze. The chief difficulty to which attention has to be paid with such a brush is the uneven wear of the copper and the carbon.

The "Endruweit" (or "Galvano" brush, as it is sometimes called) is manufactured by The Galvanic Metal Paper Company of Berlin. In this type of brush the carbon is interleaved with exceedingly thin laminations of copper. The conductivity of the brush can be varied by altering the relative proportions of copper and carbon. When a very high conductivity brush is required, so much copper is used that, if the brush be at all thin, it is quite pliable in spite of the alternate layers of carbon. From an examination of such brushes, it appears that they are probably formed by a continuous winding of copper foil interleaved with paper. When the material has attained sufficient size, it is finished off with a final layer of copper, and is subjected to a high pressure and brought to the required shape. Meanwhile the paper is carbonised by an incandescent process. The resulting brush shows quite distinctly the alternate layers of copper and carbon. This process would probably be unsuitable for brushes with only a small proportion of copper; in this case an examination of the brushes indicates that resort has been made to electrolytically depositing copper on thin slabs of carbon. These component slabs are then suitably combined into a stratified brush by an incandescent process which the makers do not divulge.

The most important consideration in the use of these brushes is to keep the commutator surface in good condition. Unless the surface is both smooth and true, trouble is experienced from the rapid and uneven wear either of the brushes or of the commutator, according to the relative hardness of each. "Endruweit" brushes are widely employed on turbo-generators, and they have been found to give good satisfaction on commutators with a good polished surface.

A somewhat similar development for the carbon brush has been instituted by the Morgan Crucible Company, who, amongst their many types of brush, manufacture graphite brushes with a distinct grain, such that the conductivity along the grain is very much higher than the conductivity across the grain. The specific resistance varies with the actual grade employed, but it is of the order of 2000 and 16,000 ohms per cubic centimetre with and across the grain respectively, so that brushes of this construction offer a resistance to transverse currents, some eight times more than the corresponding resistance

to the working currents. It must, however, be remembered that by far the greater part of the resistance is due to the resistance of contact at the surface of the brush, so that the advantage gained by the use of such brushes is not as large as might appear at first sight. Such brushes have, however, sometimes been found useful in large machines with poor commutating constants, and have found consid-

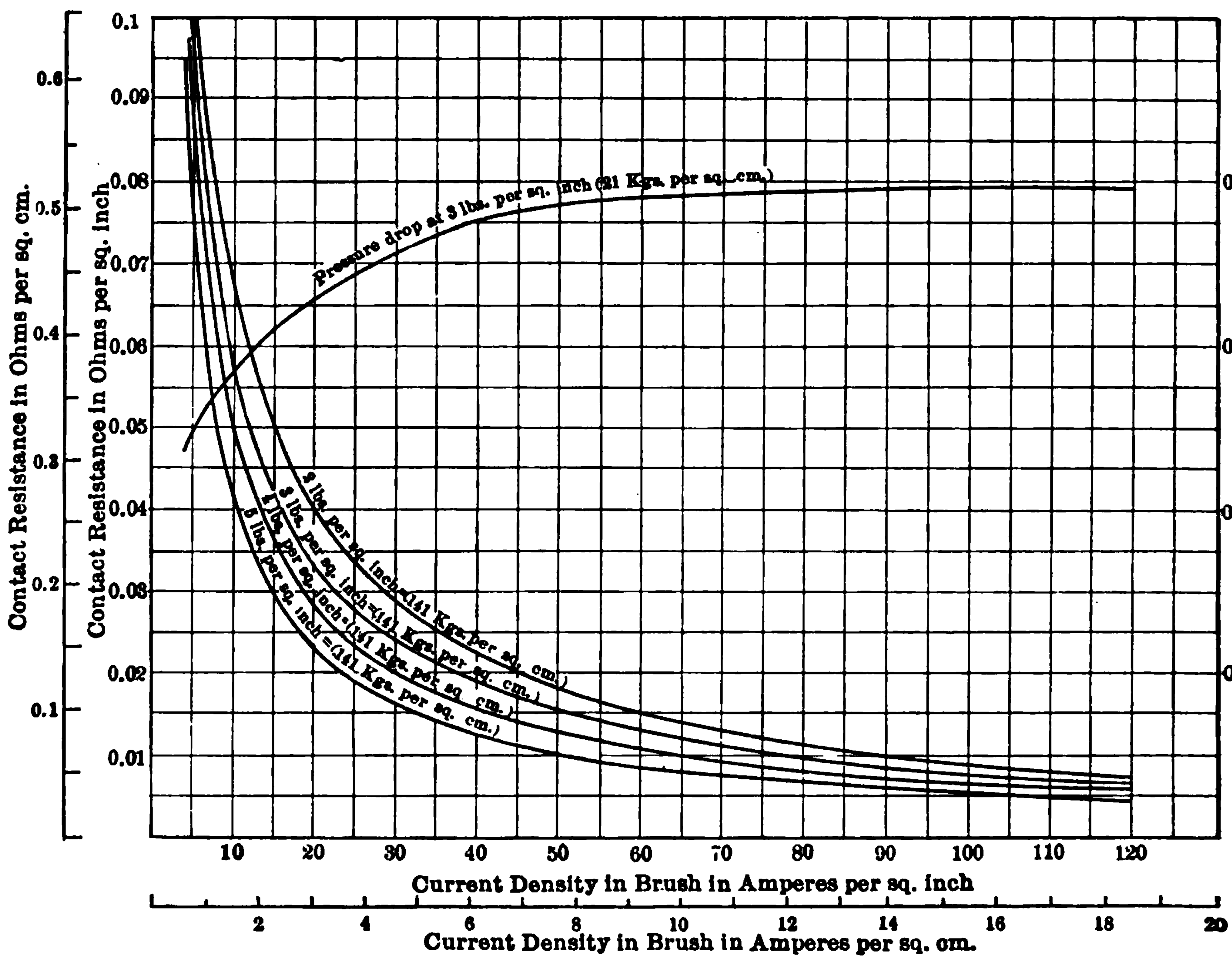


FIG. 343. — Variation of contact resistance and voltage drop with current density for Morganite brushes.

erable favour. These brushes afford a very good instance of another important condition that affects the satisfactory commutation of a machine; the material is somewhat softer than the ordinary carbon used for brushes, and has a lower coefficient of friction; the results, however, show that unless the commutator has a *clean, smooth surface,* they may be even less satisfactory than ordinary brushes. In very many cases, however, excellent results are being obtained with Morganite brushes.

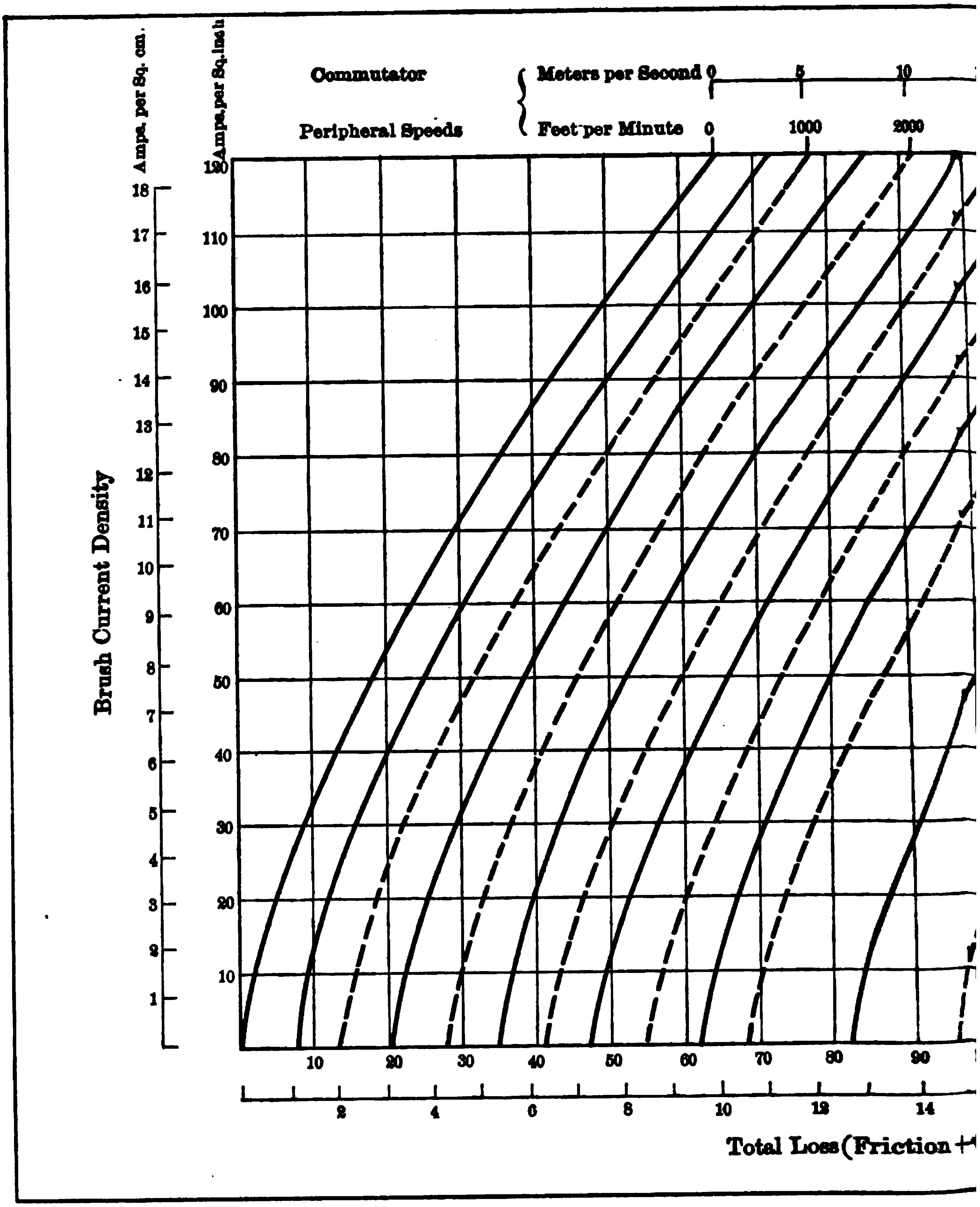
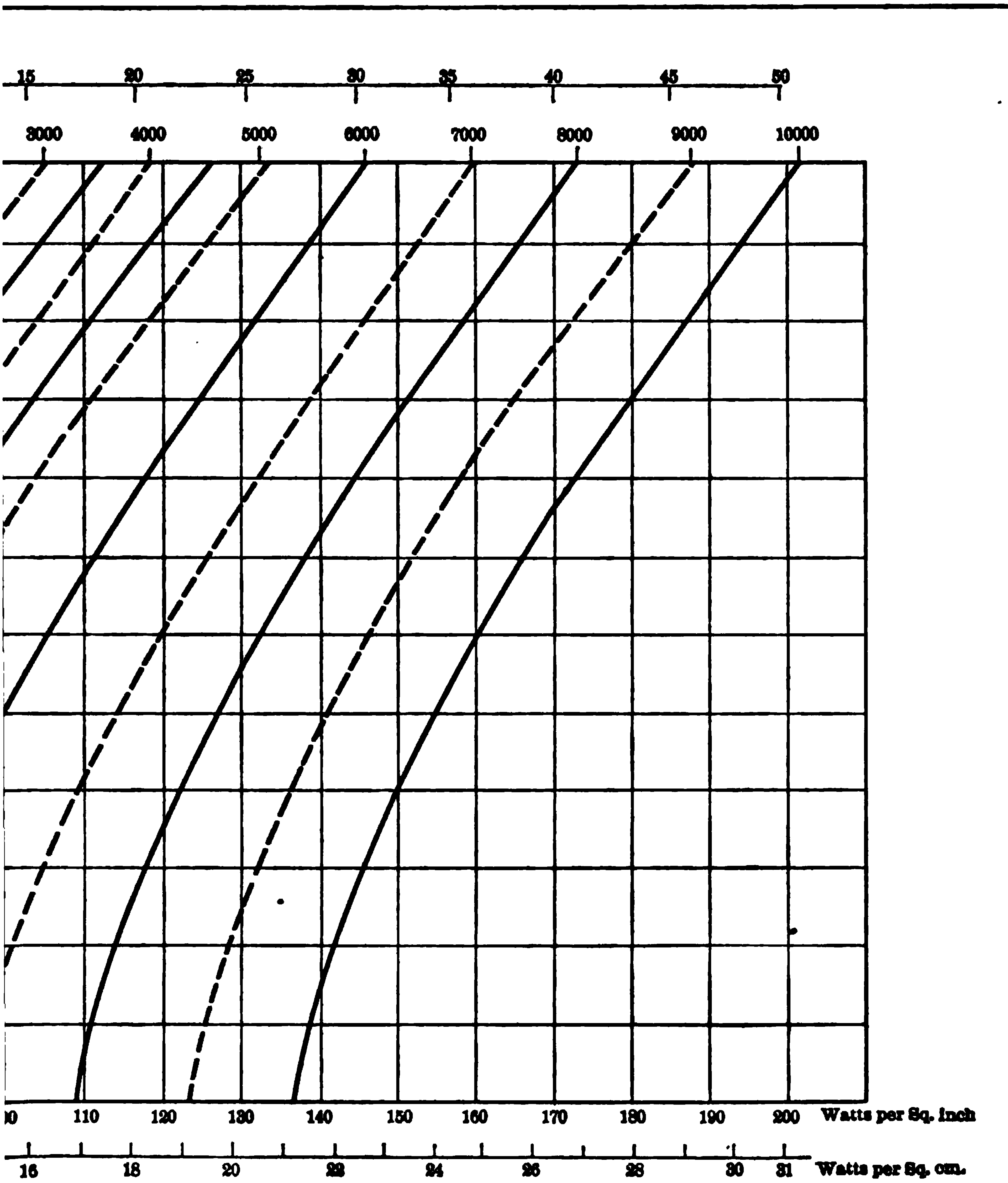


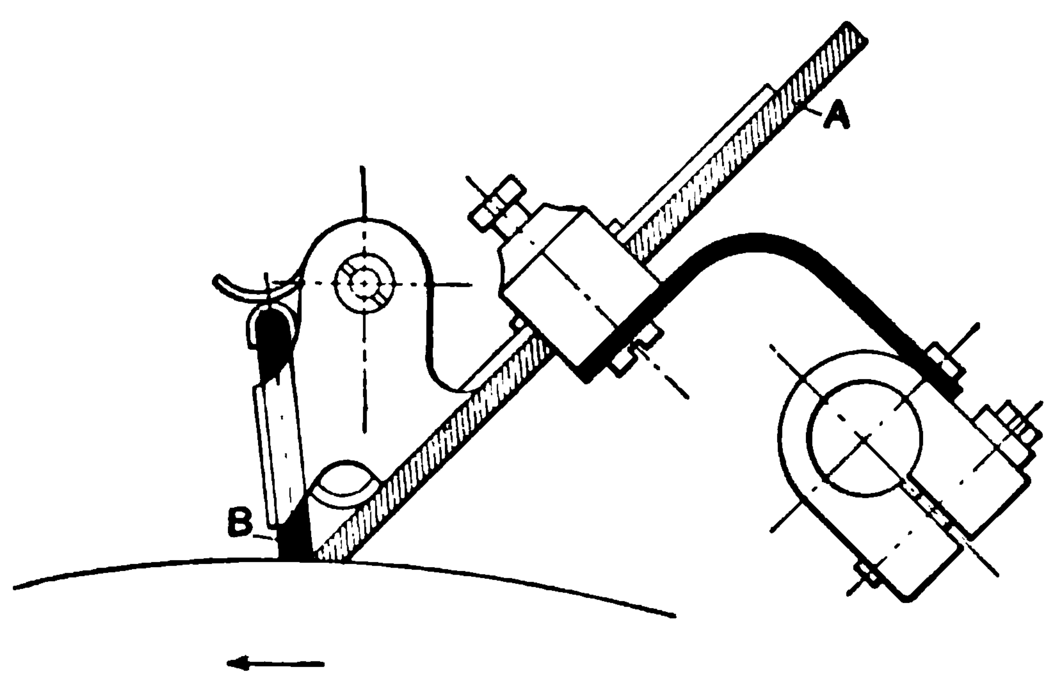
FIG. 344. — Curves for obtaining the



ontact Resistance [$I^2 R$]

mutator losses with Morganite brushes.

The curves shown in Figs. 343 and 344 have been kindly placed at our disposal by the Morgan Crucible Company. Fig. 343 shows the contact resistance and voltage drop for various pressures and densities. In the experiments, a carbon brush was used on a copper ring, and the current in each case was flowing from brush to ring; the curves apply for all speeds between 5 and 50 metres per second. Fig. 344 represents the total commutator losses for different speeds, and the curves include the loss due to the resistance of both positive and negative brushes, and also the friction losses. For all these curves a pressure of 0.2 kilogram per square centimetre, a friction coefficient of 0.2 or less, and a temperature rise of 45 degrees C. are assumed.



A-Copper
B-Carbon

FIG. 345. — Typical arrangement of brush gear for pilot brushes.

Ceylon graphite is stated to be employed in the manufacture of Morganite brushes. It is said that this is ground fine and mixed with a gelatine solution, and that the material, after drying, is mixed a second time, and is finally formed under heavy pressure. It is stated that the brushes are not baked. The degree of hardness appears to be partly controlled by the percentage of gelatine.

Among other developments in the direction of compound brushes should be mentioned the use of pilot brushes of a high resistance material, while relying on brushes of good conductivity to collect the bulk of the current. Fig. 345 is a diagrammatical indication of this arrangement. The main current passes through the copper brush "*A*," while the brush "*B*" serves as a pilot. With such an arrangement of brush gear, a wide range of combinations is made possible by the use of compound brushes. To mention one example of this,

the brushes employed on the 1000-kw. Brown Boveri generator referred to on page 431 of Chapter XVIII, consist of twelve sets of "Galvano"* metallic brushes with six sets of "Morganite" brushes as pilots.

Fig. 346 shows a type of brush gear which has been employed by Messrs. Brown, Boveri for this purpose. The brush spindles are supported by rocker rings at each end. One of these rocker rings is made specially massive in order to serve as an end shield for the stator.

Fig. 346. — Brown Boveri brush gear for turbo-generators, showing pilot brushes.

It will be clearly seen from this figure that several of the brush holders are built to hold a pilot brush as well as a main brush.

The use of pilot carbons is somewhat analogous to the use of sparking blocks on switch gear, but there is this difference in the conditions: in a switch, all the current is flowing in the same direction, whether through the carbon contacts, or through the metal contacts, but, in a commutator brush, the transverse currents partly assist and partly oppose the working currents. The effect of this is to make the resultant current at one tip of the brush larger than the resultant current at the opposite tip, since the contact resistance decreases with the

* Also called "Endruweit" brushes.

current density. The end of the brush tips having the larger current, due to the presence of the transverse currents, will carry a still larger proportion of the main current, owing to the fall in resistance. The effect of this is to tend to cause an undue proportion of the current to enter at one tip of the brush, the other tip being comparatively idle. Hence the contact surface that must be allowed is considerably greater than would be necessary with a uniform distribution over the entire contact surface.

Another form of brush which aims at combining the lubricating and commutating qualities of carbon with the high conductivity and low friction coefficient of metals is the " Bronskohl " brush made by the Aktiebolaget Bronskohl of Stockholm, Sweden.

This brush is made of copper and tin coated graphite powder, compressed and heated, forming a homogeneous material. It is stated that by varying the proportions and the pressure, the specific gravity, hardness, strength, conductivity and contact resistance may be suited to requirements within wide limits. The brush losses are reduced to a large extent by this brush as shown by the curves in Fig. 347, which are published by the manufacturers. The dotted curves relate to ordinary carbon brushes, and the full line curves to Bronskohl brushes. The voltage drop at the brush contacts is stated to be 0.2 volt for these brushes, at a current density from 30 to 50 amperes per square centimetre.

Brush Holders. — The design of the brush gear for high speed machinery is a matter of first importance. The chief points to be observed in the construction of the brush gear may be briefly stated as follows: —

(*a*) *The collection of the current from the brush.* Flexible copper connections are almost always employed for carbon brushes, and the most important consideration is the method of fixing the connection to the brush. Solder is often employed, but it is not to be recommended, and should be replaced by a mechanical clip of some kind.

(*b*) *The pressure on the brush.* This is most frequently obtained from some form of spring, which should be capable of easy adjustment. The spring must be arranged so that the wear of the carbon shall not appreciably alter the pressure at the brush contact.

(*c*) The moving parts of the brush holder should be constructed to have the smallest inertia that can be obtained without sacrificing rigidity. This consideration has frequently led to the use of

Amp/cm²

Peripheral Speed in m/sec.

Fig. 347. Brush Contact Losses for Bronskol and Carbon Brushes.

—— Bronskol

------ Carbon

aluminium, and satisfactory results have been obtained with such designs.

(*d*) Friction should be reduced to a minimum by careful design and workmanship.

(*e*) It is preferable to have the required aggregate contact surfaces made up from as many independent component brushes and brush holders as possible, so far as relates to obtaining the best results when each brush is at its best adjustment.

The Collection of Current from the Brush. — In brush holders of the types shown in Figs. 349 and 350 the brush is in rigid and good electrical connection with its holder. If the arm is free to move on the spindle the current has to pass through the working joint which is not in itself of sufficiently good conductivity. Hence the brush itself, or the arm to which it is rigidly connected, should be connected to the brush spindle, or to some part of the brush mechanism in rigid connection with the brush spindle. This is usually effected by means of a flexible copper connector securely fixed to the brush or to the brush arm.

In other types of brush gear where the brush is not rigidly connected to any part of the brush mechanism, as in Figs. 351 and 352, the flexible connector must be securely attached to the brush itself.

The mode of attachment of the connector or " pigtail " is a problem in the manufacture of brushes. Solder has been employed to fix the connector direct to the brush or to a surface of the brush through the medium of a copper plate. This method is unsatisfactory, as the solder is liable to melt when the brushes heat.

A variety of alternative methods of attachment have been employed which consist chiefly either in the embedding of the pigtail in the carbon, or clamping it on with some form of metal clip. In the former class the pigtail is generally embedded during the making of the brush.

A number of methods are illustrated in Fig. 348. Fig. (*a*) shows a simple flexible embedded in the carbon. Fig. (*b*) represents a method employed in some of the Morganite brushes; the pigtail is inserted in a hole in the brush, the hole being slightly larger than the flexible cable; the latter is spread at its lower end, and the intervening space filled with carbon pressed in tightly. Fig. (*c*) illustrates a type known as " expansion pigtail " of the National Carbon Company. Figs. (*d*) and (*e*) are methods employed by the same firm in which a

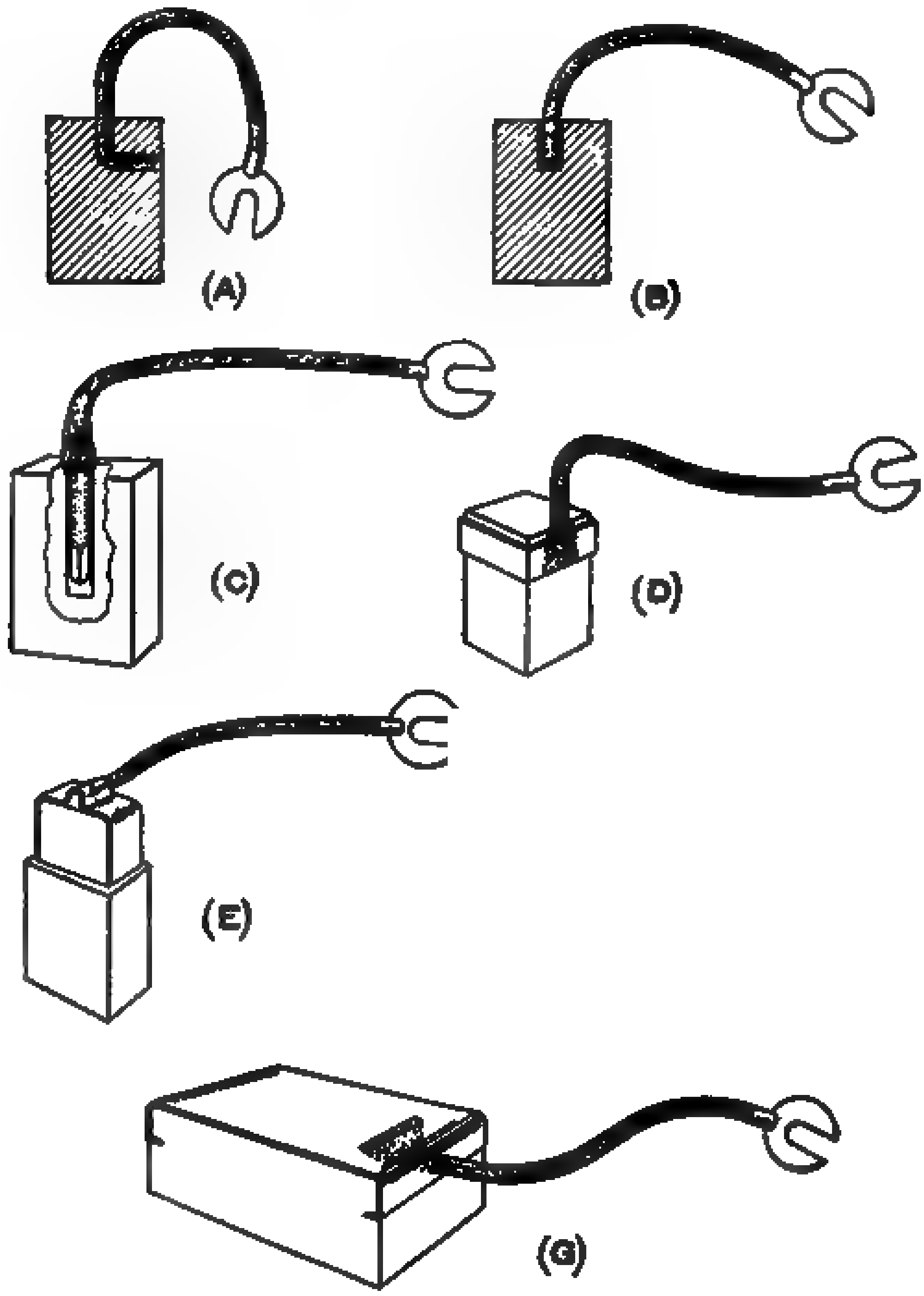


Fig 348 - Methods of Attaching Flexible Connectors to Carbon Brushes

metal clip is utilised to clamp the pigtail on to the brush. Fig. (*f*) shows a method employed by the Le Carbone Co. Fig. (*g*) illustrates a method which has been employed with graded Morganite brushes. The clip of metal covering the whole of the upper surface of the brush ensures that the current is carried by the entire brush. It was found that with these graded brushes the current tended to concentrate at the section of least resistance and the construction employed ensures that the flexible connector makes contact with the entire surface of the brush.

The chief points to be observed in the attachment of the pigtail comprise making a sound mechanical and electrical joint which will not give way from any causes, such as vibration or heating.

The Pressure on the Brush.—The various common types of brush holders may be classified into four standard types:

1. Arm or lever type.
2. Spring arm type.
3. Box type.
4. Reaction type.

1. The arm or lever type consists essentially of two parts, one free to rotate about the brush spindle, and the other capable of being rigidly clamped to the brush spindle. The former constitutes the arm and carries the brush at its end; the latter part is connected to the arm through the medium of a spiral or flat spring by means of which pressure is exerted on the brush. The pressure is varied by adjusting the position on the spindle of the second part which is clamped thereto. In this type of holder the pressure is obtained through the medium of the lever, and as the brush wears, it moves about the brush spindle as centre. Fig. 349 illustrates a brush holder of this type with a flat spring for the pressure.

2. The spring arm type consists, when assembled, of one rigid piece as indicated in Fig. 350. The arm of the brush holder consists of a flat spring, one end of which carries the brush and the other end of which is clamped on to the brush spindle. The pressure is exerted by the springy action of the arm. It is varied by varying the position of the clamped end on the brush spindle.

3. The box-type holder consists mainly of a neutral box enveloping the brush and open at both ends to permit the brush to project and press radially on the commutator. Pressure is exerted directly on

Fig 349 Arm Type

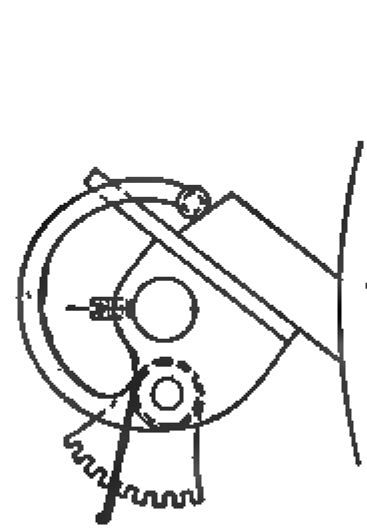


Fig 351 Box Type

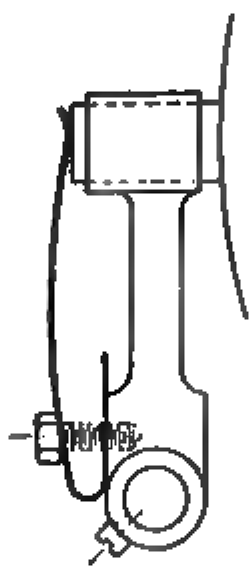


Fig 350 Spring Arm Type

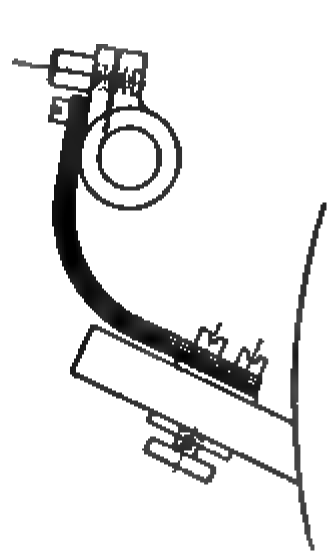


Fig 352 Reaction Type

Figs 349-352 Types of Spring Pressure Brush Holders.

the end of the brush projecting through the outer end of the box, by means of a spiral or flat spring, the brush being free to slide into the box as it wears away. Fig. 351 illustrates a brush holder of this type.

4. The reaction type, which is illustrated in Fig. 352, consists of a piece of metal rigidly fixed to the brush spindle, and against one face of which the brush rests. Pressure is obtained at the outer end of the brush by means of the pressure of a spiral spring exerted on an arm piece which is free on the brush spindle. This arrangement presses the brush on the commutator, and also holds it in its place against the metal foundation. The pressure on the brush is varied by altering the position of the spring in the notches cut in the arm piece for that purpose. The direction of rotation of the commutator should be that indicated by the arrow, as otherwise the action is frequently not smooth.

The designs shown in Figs. 349–352 are typical, practical examples of these types. A very large number of brush-holder designs are in use, but they are mostly modifications, to a greater or less degree, of the four above types.

In both types (1) and (2) the pressure is obtained through a spring, one end of which is clamped rigidly to the brush spindle. Consequently, as the brush wears, the pressure decreases and adjustment becomes necessary.

In types (3) and (4) the pressure is exerted directly upon the brush through the medium of the spring, and the pressure is unaffected by the wear of the brush (until the wear becomes excessive and the brushes require renewal).

It is desirable to arrange for the pressure to be applied directly to the brush, and preferably in the direction of the axis of the brush. In this case the pressure is radial on the commutator and evenly distributed over the whole of the brush surface, so that the wear is also evenly distributed. In brush holders of the types (1) and (2) above, the wear is greatest at the edge of the brush nearest the spring or the brush spindle. Furthermore in types (3) and (4) the brush itself is free to move as it wears, and there are no other parts of the brush mechanism rigidly connected to it. In designs where the brush is left free in this way, the moving parts have the smallest inertia, and respond more readily to variations in the truth of the commutator.

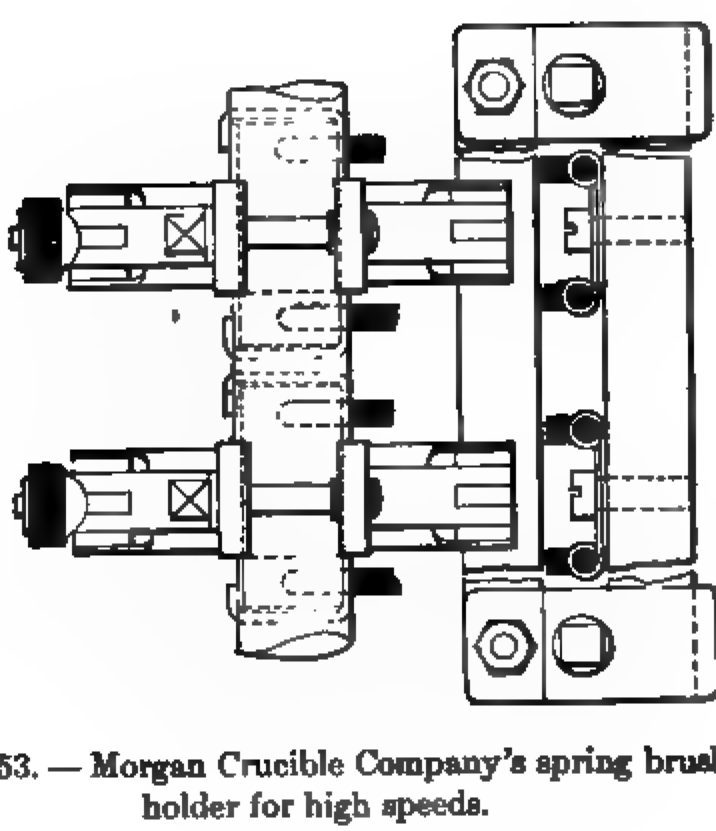


FIG. 354. — Section of the Morgan Crucible Company's pneumatic brush holder.

FIG. 353. — Morgan Crucible Company's spring brush holder for high speeds.

The above discussion relates to brush holders in which the pressure is obtained by means of a spring. Another example which varies considerably in constructional detail, but is based on the same general principles as the above, is illustrated in Fig. 353.

For a more detailed analysis of the design of brush holders, reference should be made to some of the standard works on Dynamo Design. It should be mentioned, however, that most of the difficulties generally discussed in these treatises are greatly intensified in high speed machines. As an example of a brush holder designed for peripheral speeds up to 40 metres per second, Fig. 353 shows a type employed by the Morgan Crucible Company. The brush holders are mounted on a rod of **D** section, and a similar rod conveys the current from the flexible connectors. The pressure is obtained from a spring, as shown, and is conveyed by means of links, which produce a parallel motion for the brush.

In a more elaborate design by the same firm, the pressure is obtained by means of compressed air, which provides a very flexible method of adjusting the pressure on the brushes, and avoids the difficulty of the pressure altering as the brush wears away. A section of the cylinder is shown in Fig. 354. The compressed air is led through a series of channels to the rubber bag K, and exerts a downward pressure on the plunger F; this pressure is transmitted by a close wound steel spring G to an ebonite nipple H which is spherically shaped at the lower end, to bed into the top of the carbon brush as shown in Fig. 355.

The rubber bag K is coated with silk and is provided with a sealing valve to isolate the holder in the event of the bursting of the bag. This is obtained by the action of the rubber diaphragm between the washer L and the plug M; the normal path for the air is through a channel or groove in the plug J, past the brass washer L and the rubber diaphragm, through a similar channel in the plug M, and finally through the small hole to the bore of the plug, whence it has easy access to the interior of the rubber bag. The rubber diaphragm, being subject to equal pressures on each side, will remain flat, as shown in the drawing. If, however, the rubber bag bursts, the difference in pressure between the two sides of the diaphragm causes it to bulge downwards, and this at once closes the small hole in the plug M.

The effect of this valve is to automatically cut off the supply of

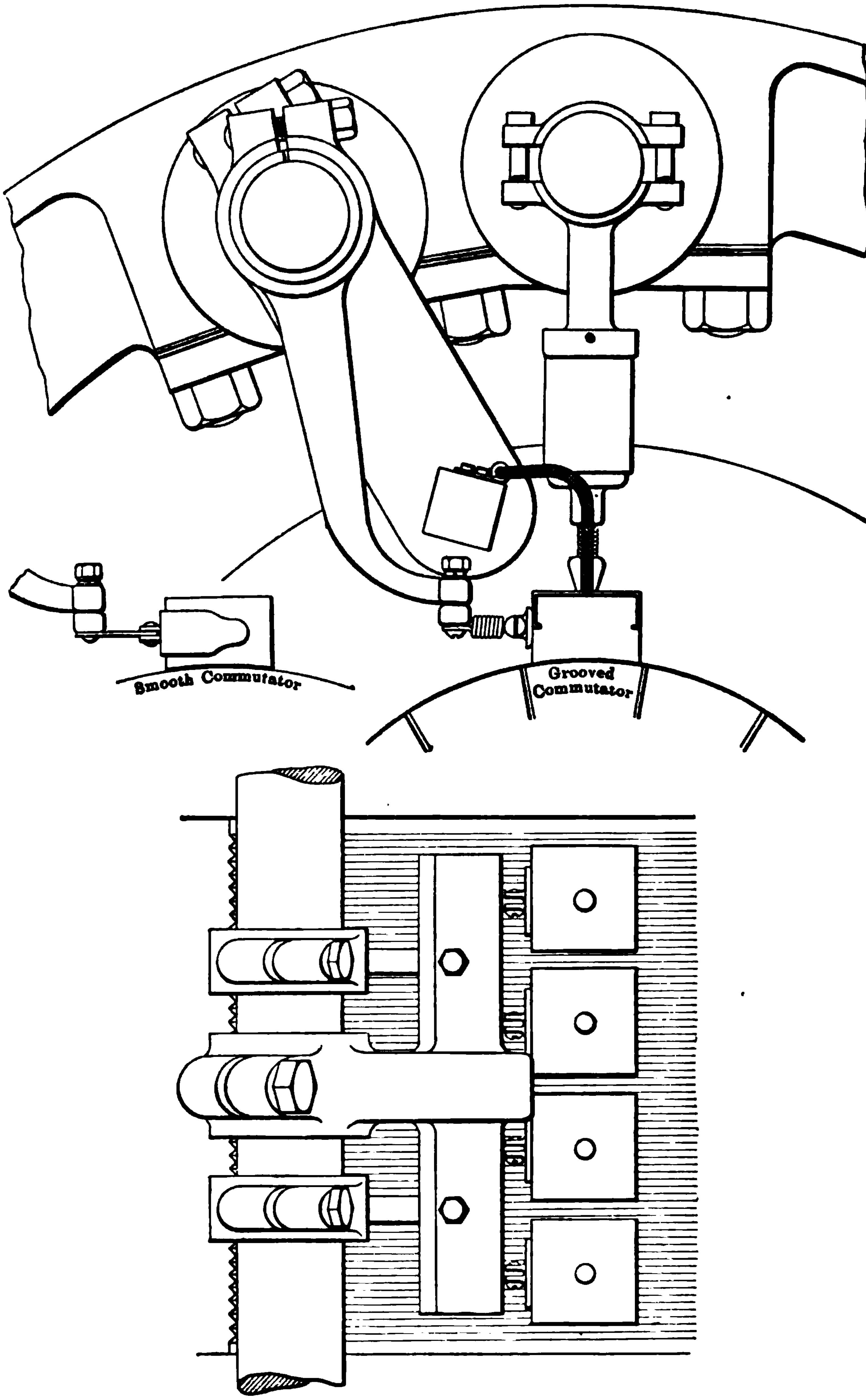


Fig. 355. — Morgan Crucible Company's pneumatic brush holder.

air to a holder as soon as the bursting of the bag produces this difference of pressure. The plunger F also contains a groove (not shown in the figure) which by means of a screw in the cylinder E provides a stop to limit the travel of the plunger.

Fig. 355 shows this type of brush holder fitted to a grooved commutator, which is a device that has often been tried with the object of avoiding excessively long commutators. It is very doubtful, however, whether it is good practice to use these grooved commutators. While they reduce the first cost of the machines, it is doubtful whether they commutate so successfully as smooth commutators.

Pneumatic brush holders have, no doubt, several advantages, but it should be remembered that a further source of breakdown is introduced; and though breakdowns to the pneumatic plant may be improbable, yet when they do occur the generator is put completely out of action. It occurs to us, however, that such a holder might be provided with springs in case of such emergency, the springs being out of action so long as the pneumatic pressure is in working order.

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BY E. S. WALPOLE.

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